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**Development of a Current
Collection Loss Management
System
for SDI Homopolar Power
Supplies**

Annual Progress Report #2 Covering the Period September 30, 1987
to December 31, 1989

Prepared for

U. S. Dept. of Energy
San Francisco Operations Office
1333 Broadway
Oakland, CA 94612

Under Contract No. DE-AC03-86SF16518
G.O. No. SF-11856-CE

by

Donald W. Brown, Principal Investigator

Technical Contributors: W. F. Hannan III, D. Pavlik,
E. S. Ortolini, A. Patterson, and G. T. Mallick

Westinghouse Electric Corporation
Science & Technology Center
Pittsburgh, PA 15235

Westinghouse STC
1310 Beulah Road
Pittsburgh, Pennsylvania 15235

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Westinghouse STC
1310 Beulah Road
Pittsburgh, Pennsylvania 15235

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ENGINEERING TECHNOLOGY DIVISION

ETD 2684

STC Report 90-9E8-CLCTR-R1

Contract Required

DEVELOPMENT OF A CURRENT COLLECTION LOSS
MANAGEMENT SYSTEM FOR SDI HOMOPOLAR POWER SUPPLIES

Written by:

Donald Brown

D. W. Brown
Electromechanical Applications

Reviewed by:

O. S. Taylor

O. S. Taylor, Manager
Electromechanical Applications

Reviewed by:

C. J. Heyne

C. J. Heyne, Manager
Electromechanical Systems

Approved by:

F. T. Thompson

F. T. Thompson, Manager
Engineering Technology Division

Westinghouse Electric Corporation
Science and Technology Center
1310 Beulah Road
Pittsburgh, Pennsylvania 15235

TABLE OF CONTENTS

	<u>Page</u>
LIST OF FIGURES	ii
LIST OF TABLES.....	iii
EXECUTIVE SUMMARY	iv
ABSTRACT	vi
1. INTRODUCTION	1-1
1.1 BACKGROUND	1-1
1.1.1 Homopolar Power Supply	1-1
1.1.2 Current Collection Technology	1-3
1.2 TECHNICAL APPROACH	1-4
1.2.1 Objectives	1-4
1.2.2 Approach	1-4
1.2.3 Goal	1-4
2. PROJECT ACCOMPLISHMENTS	2-1
2.1 CRYOGENIC CURRENT COLLECTION TEST RIG	2-1
2.1.1 Background of Cryogenic Testing	2-1
2.1.2 Cryogenic Test Rig Design	2-1
2.1.3 Tests	2-3
2.1.4 Results	2-5
2.2 HIGH SPEED TEST RIG	2-6
2.2.1 Background of High Speed Test Rig	2-6
2.2.2 High Speed Test Rig Electrical Design	2-11
2.2.3 High Speed Test Rig Mechanical Design	2-12
2.2.4 Data Acquisition System	2-16
2.2.5 High Speed Test Rig Status	2-17
2.3 HPG LOSS ANALYSIS	2-22
2.3.1 Summary	2-22
2.3.2 Status	2-25
2.3.3 Rotor Heating	2-25
2.4 HOMOPOLAR OPTIMIZATION STUDIES	2-25
3. PRIOR ACCOMPLISHMENTS	3-1
3.1 HOMOPOLAR MACHINE APPLICATION STUDY	3-1
3.1.1 Background	3-1
3.1.2 Conclusions and Recommendations	3-3
3.2 ROTOR CONDUCTOR CONFIGURATION	3-5
3.2.1 Rotor Stress	3-5
3.2.2 Heat Transfer	3-5
3.3 SHUNT-BRUSH DIFFUSION BOND	3-7
4. SCHEDULE/STATUS	4-1
5. FUTURE EFFORTS	5-1
5.1 TEST PROGRAM	5-1
5.2 DETAILED LOSS ANALYSIS	5-1
5.3 PROTOTYPE CURRENT COLLECTION SYSTEM	5-1
6. ACKNOWLEDGMENTS.....	6-1
APPENDIX A.....	A-1
APPENDIX B.....	B-1
APPENDIX C.....	C-1

LIST OF FIGURES

	<u>Page</u>
Figure 1.1 — High power density homopolar generator.	1-2
Figure 2.1 — Schematic of liquid nitrogen-cooled test rig.	2-2
Figure 2.2 — Liquid nitrogen-cooled test rig under operation. ..	2-4
Figure 2.3 — Relationship between positive voltage drop and temperature for brush grade W405.	2-7
Figure 2.4 — Relationship between negative voltage drop and temperature for brush grade W405.	2-8
Figure 2.5 — Relationship between double voltage drop and temperature for brush grade W405.	2-9
Figure 2.6 — Completed rotor for high speed test rig.	2-13
Figure 2.7 — High speed test rig schematic.	2-14
Figure 2.8 — Completed brush rings.	2-15
Figure 2.9 — IBM PC driven data acquisition system.	2-18
Figure 2.10 — Assembled test rig.	2-19
Figure 2.11 — Boundary conditions on surface are expressed as limits on radial currents over outer surface and condition of no axial currents from ends.	2-23
Figure 2.12 — Surface plot showing radial current injected at cylindrical surface.	2-24
Figure 3.1 — Ideal drum type homopolar motor.	3-2
Figure 3.2 — Micrograph of the interface between the #359 Cu-C and copper.	3-8
Figure 3.3 — Micrograph of the interface between the #933 Cu-C and copper.	3-9
Figure 4.1 — Current collection program schedule.	4-1

LIST OF TABLES

	<u>Page</u>
Table 2.1 — BRUSH GRADES TESTED ON LIQUID NITROGEN-COOLED TEST RIG	2-5
Table 2.2 - HIGH SPEED TEST RIG MOTOR SPECIFICATIONS	2-11

EXECUTIVE SUMMARY

Various weapons systems under consideration for the Strategic Defense Initiative (SDI) will require large amounts of electrical power (hundreds of megawatts). A large portion of that electrical power is dc, depending upon the particular application. This program supports the development of enabling technologies for large dc power supplies for SDI, specifically the homopolar generator.

An example of one such weapons system to be identified is the neutral particle beam (NPB). NPB is the application chosen for analysis and comparison in this program. RF power generation is the major consumer (~90%) of NPB burst mode power. It also occupies a major portion of the platform mass (up to 60%). Since RF generation comprises the largest component of power consumption, it is important to develop efficient, lightweight power supplies to operate them.

Four specific areas have been identified which are considered "enabling technologies" necessary to construct spaceborne homopolar power supplies. These areas are as follows:

- Steady-state operation of current collectors at cryogenic temperatures
- Rejection of the immense heat flux occurring at the slip ring surface
- Identification of all appropriate loss mechanisms
- Modeling of the underlying electromagnetics of high powered homopolar devices

Work done on this contract has advanced and will continue to advance the state of the art in high powered space power supplies. Specific accomplishments are as follows:

- Construction of the first high current density current collection test rig to demonstrate operation at cryogenic temperatures.
- Successful operation of this test rig and progress toward characterization of collector behavior at cryogenic temperatures.
- Construction of a high speed current collection test rig capable of rubbing speeds of 200 m/s and current densities of 2 kA/cm^2 , with 20% brush coverage.
- Identification of pool boiling of available cryogenic fuel in the centripetal field of the rotor conductor as adequate to reject the large heat fluxes required.
- Identification of a significant loss mechanism driven by a "skin effect" shown to develop in high velocity current collectors utilizing monolithic brushes.
- Creation of an electromagnetic model to optimize air-core homopolar machines. A significant, surprising result of this study demonstrates that such machines are optimized at operational magnetic fields of $\sim 2 \text{ T}$, which is much lower than previously assumed.
- Tradeoff analysis showing that air-core homopolars can achieve power densities of 80 kW/kg at a power level of 50 MW, and with an efficiency of 97%. This performance satisfies the weight and power goals desired for such devices.

Work remaining in the last year of this contract will demonstrate high speed, high current density operation of a current collector utilizing the high speed (200 m/s) test rig. Knowledge gained during this test and the cryogenically cooled tests will be used to construct a prototype current collection system capable of operating in a cryogenically cooled environment with a rubbing speed of 200 m/s, a current density of 2 kA/cm^2 , and a packing factor of 20%.

Significant progress has been made toward developing the enabling technologies needed to operate high powered homopolar power supplies in the space environment.

ABSTRACT

High speed, high power density current collection systems have been identified as an enabling technology required to construct homopolar power supplies to meet SDI missions. This work is part of a three-year effort directed towards the analysis, experimental verification, and prototype construction of a current collection system designed to operate continuously at 2 kA/cm^2 , at a rubbing speed of 200 m/s, and with acceptable losses in a space environment. To date, no system has achieved these conditions simultaneously.

This is the annual report covering the second year period of performance on DOE contract DE-AC03-86SF16518.

Major areas covered include design, construction and operation of a cryogenically cooled brush test rig, design and construction of a high speed brush test rig, optimization study for homopolar machines, loss analysis of the current collection system, and an application study which defines the air-core homopolar construction necessary to achieve the goal of 80-90 kW/kg generator power density.

1. INTRODUCTION

1.1 BACKGROUND

1.1.1 Homopolar Power Supply

Homopolar machines have been used for over a decade as reliable high current pulsed power supplies for electromagnetic launchers (EML), resistance welding, and metal compaction. In the 1970s, several iron-core homopolar machines were built as power supplies for laboratory EML systems. Although these machines satisfactorily performed their functions, their power density was low ($< 10 \text{ kW/kg}$) and they could produce only one short-duration pulse every several hours. The movement of these power supplies to space requires a new approach with high power density, acceptable efficiency, and the capability of essentially steady operation for rapid fire. One new approach is an air-core homopolar employing cryogenically cooled coils (hyperconducting or superconducting).

The voltage developed by a homopolar machine is proportional to the machine active length, the magnetic field intensity, and the rotor tip speed. Iron-core machines are limited to a magnetic field strength of about 2.0 T; the rotor tip speed is limited by the strength of the ferromagnetic material in the rotor. In an air-core machine, for the same active length, fields of as high as 8 T and possibly higher can be achieved and rotor tip speeds can be increased through the use of unique rotor designs and high strength, light weight materials. A section of an air-core drum type homopolar is shown in Figure 1.1. The drum type configuration was chosen because it produces higher power density and generated voltage than other configurations, based on a study in which Westinghouse was a principal contributor. The machine in Figure 1.1 represents a module consisting of two counter-rotating rotors designed to eliminate any net torque on the space vehicle foundation.

Figure 1.1 — High power density homopolar generator.

These modules can be connected in series or parallel to achieve the voltage or current required. Using this approach, the power densities required for SDI (80-90 kW/kg) can be achieved. The success of this approach is dependent on the development of a current collection system that will: (1) function in the machine, (2) operate in the environment, (3) pass current at a high density (2 kA/cm^2) and high rubbing speed (200 m/s) continuously, (4) have acceptable losses, and (5) operate near the temperature of the coolants available (possibly liquid hydrogen or liquid oxygen). Such a current collection system is not yet available.

1.1.2 Current Collection Technology

Significant advancements in conventional electrical brush contacts have resulted from research and development programs, such as those at the Westinghouse Science & Technology Center (formerly the R&D Center), during the past forty years. Within the past ten years, advanced machine and device applications have been identified which require brush current densities for continuous duty 10 to 50 times higher and for intermittent or pulsed duty up to 5,000 times higher than for the highest conventional sliding brush contact system. Through intensive R&D effort, achievement of these brush-ring contact, high current density goals has been demonstrated with laboratory test apparatus and with actual prototype machines and devices. The high contact current densities are achieved by employing selected brush and ring (or rail) materials, unique brush configurations, conventional to special cooling, and controlled ambient gas/vapor environments.

Although current collection systems have been developed for various applications requiring high current density, high speed, and steady-state operation, a single system has yet to be developed which meets all of these requirements simultaneously. In this context, steady-state operation (in terms of temperature rise) is acknowledged to be reached in 30 to 300 seconds. This void in the technology presents a significant challenge to machine applications which require operating times of the order of minutes.

1.2 TECHNICAL APPROACH

1.2.1 Objectives

The objective of this program is to produce a current collection system that will function as part of an air-core homopolar power supply designed to operate in space. The current collection system will be designed to meet certain specific technical goals including: continuous operation at high current density (2 kA/cm^2), high rubbing speed (200 m/s) in a high local magnetic field (2 to 6 T) with acceptable losses and wear associated with the extreme reliability demanded of space equipment.

1.2.2 Approach

The program to develop this current collection system consists of two parallel parts: (1) the development of a cryogenic brush slip-ring combination for high speed and high current density, and (2) the development of a cooling system that is capable of continuously removing the losses generated. The results will be incorporated in the design of a current collection system capable of operating in an air-core homopolar power supply for continuous operation in space. A prototype current collection system will be fabricated and tested to confirm its operational characteristics.

1.2.3 Goal

The program goal is to analyze, design, build, and test a continuously operating ($> 30 \text{ s}$) current collection system with a 200 m/s rubbing velocity, 2 kA/cm^2 current density in the current collector, and with a 0.2 packing factor.

Packing factor is defined as the ratio of the gross rubbing area of the current collector divided by the area of the rotor swept by the current collector.

Additional goals of the program are to demonstrate analytically that such a current collection system will operate in the expected magnetic fields occurring in such a high powered device, and that homopolar power supplies meeting power densities of 80-90 kW/kg are practical utilizing such systems.

2. PROJECT ACCOMPLISHMENTS

2.1 CRYOGENIC CURRENT COLLECTION TEST RIG

2.1.1 Background of Cryogenic Testing

Heat rejection from space-based homopolar power supplies will be accomplished utilizing liquid hydrogen which will then fuel the prime mover. The utilization of this cryogenic fluid to cool the current collection system will impose operational temperatures much lower than ever experienced in high speed, high power density current collection devices.

Conventional sliding contacts rely upon establishing a thin, conductive film to provide a suitably conductive, lubricated interface. Such films are normally formed of graphite present in conventional brushes acting in the presence of water vapor or certain hydrocarbon vapors. With the exclusion of such vapors from a conventional brush, the graphite film becomes abrasive, and rapid wear results.

Special brushes utilized for high altitude or extreme cold have been developed to allow the formation of conductive films under adverse conditions. These brush materials have special additives, such as lithium flouride, which assist in film formation.

The brush materials tested on this test rig were selected from commercially-available, high altitude brush grades manufactured by the Stackpole Carbon Company. The cryogen used was liquid nitrogen.

2.1.2 Cryogenic Test Rig Design

The design of the cryogenically cooled slip-ring is shown in Figure 2.1. In operation, liquid nitrogen is fed on axis into the inner chamber of the slip-ring. From there it flows off axis into a baffle which directs the stream towards the far end of the chamber, where the stream again is diverted, into the outer chamber and reversed. The

Figure 2.1 — Schematic of liquid nitrogen-cooled test rig.

outer chamber is where the pool is formed. The warmer vapors or liquid overflow is vented through a tube which is coaxial to the inlet bayonet. Two thermocouples have been placed diametrically opposite on the rear surface of the slip-ring, near its outer radius. Two potential leads have been similarly connected.

2.1.3 Tests

The cryogenically cooled test rig was successfully operated over a period of 9 months beginning in July 1988. This rig is capable of testing various brush and slip combinations for which the collector surface is at or near the temperature of liquid nitrogen. This rig can provide a rubbing speed up to 16 m/s.

An IBM PC-driven data acquisition system was employed to monitor various operating parameters. Among these were the positive, negative, and double voltage drops, brush current, and the drive motor's voltage, current, and speed. These measurements are to be used to characterize the contact drop and friction coefficient at the rubbing interface.

In this context, positive voltage drop is the voltage drop at the collector surface where current is flowing into the rotor. Negative voltage drop is the voltage across the collector surface at the point where current is flowing out of the rotor. It follows that the double voltage drop is the absolute value of these added together.

Temperature measurements were also taken. Two thermocouples were mounted on the slip ring; one thermocouple was mounted in each brush and one was mounted on the shunt to the positive brush. Additional thermocouples were used to monitor the ambient temperature inside the bell jar, the gas temperature at the liquid nitrogen trap, and the liquid nitrogen bath temperature.

Figure 2.2 shows the test rig operating with liquid nitrogen cooling.

It should be noted that all of the measurements were run through a 5-point weighted average software filter. Consequently, the data is more representative of the mean than of an instantaneous value. Since the interest is in a gross characterization, oscillations about the mean were not considered as important as the mean itself.

Figure 2.2 — Liquid nitrogen-cooled test rig under operation.

2.1.4 Results

All testing was performed on an ETP copper rotor. The different brush grades tested along with their constituents are listed in Table 2.1.

Of the grades shown in Table 2.1 only three are considered usable. Among these, grade W405 is the most promising, with grades 564 and 5081 the second and third choices respectively.

Table 2.1 — BRUSH GRADES TESTED ON LIQUID
NITROGEN-COOLED TEST RIG

Grade	Constituents		Comments
5081	Copper	65.0%	Acceptable voltage drop and wear - some arcing at higher current densities
	Barium Flouride	4.8%	
	Graphite	30.2%	
SM338	Silver	90.0%	High wear at room temperature
	Graphite	8.0%	
	MoS_2	2.0%	
W405	Copper	86.4%	Acceptable voltage drop and wear - some arcing after 2 hours of running
	Lithium Carbonate	0.5%	
	Lead	10.0%	
	Graphite	3.0%	
SM429	Silver	90.0%	Unrepeatable results
	MoS_2	10.0%	
SM348	Silver	60.0%	Excessive voltage drop makes this impractical for high current densities
	Graphite	35.0%	
	MoS_2	5.0%	
SM489	Silver	*	Excessive voltage drop and high wear at R.T.
	Niobium Diselenide	*	
564	Copper	86.4%	Acceptable voltage drop and acceptable wear
	Lead	10.0%	
	Graphite	3.0%	
	Barium Flouride	0.5%	
359	Copper	86.4%	Excessive voltage drop
	Lead	10.0%	
	Graphite	3.0%	
	Barium Flouride	0.5%	

*Composition unknown

Figures 2.3, 2.4, and 2.5 are representative graphs of the data taken. This data will be analyzed in detail when the engineer who performed these tests becomes available. It is important however, to point out some of the trends in the data.

Each of these plots has a minimum voltage point between room temperature and -180°C . It is apparent that the film generated at the rubbing interface acts very much like a semi-conductor for slip ring temperatures below this point. The resistance rises as the temperature drops. Above the minimum, however, the converse happens, and the collector system acts more like a conductor.

The identification of this minimum voltage drop could turn out to be very useful. Each of the tested brush grades displayed this minimum, and invariably it occurred between 0°C and -50°C . On a spaced-based machine, the slip ring temperature could be maintained in this range if a complicated control system were used with the hydrogen cooling. However, a more practical application might be in ground- or sea-based systems. Relatively inexpensive commercial refrigeration systems can operate at these temperatures and no coolant control system would be required.

Another point to be made is at temperatures less than -50°C , the collector behavior is thermally stable. If the collector surface begins to heat, the resistance decreases thereby dropping the temperature. The fact that there is no thermal runaway makes the cryogenic collector system inherently safe to operate.

2.2 HIGH SPEED TEST RIG

2.2.1 Background of High Speed Test Rig

High speed, high power density current collection systems are fundamentally limited by temperature rise. It is believed that wear is strongly dependent upon the temperature experienced at the rubbing interface.

Figure 2.3 — Relationship between positive voltage drop and temperature for brush grade W405.

Negative Voltage Drop vs Temperature

Figure 2.4 — Relationship between negative voltage drop and temperature for brush grade W405.

Double Voltage Drop vs Temperature

Figure 2.5 — Relationship between double voltage drop and temperature for brush grade W405.

In order to experimentally demonstrate the operation of a current collection system, it is necessary to duplicate the actual heat loading experienced by the slip-ring. For this reason, full "coverage" of the slip-ring with the same ratio of brush area to rotor swept area is needed. Simply testing with a brush or two per end will not thermally load the collector ring to adequately demonstrate operational temperature limits.

It is also important that a high current test stand have a geometry similar to an actual homopolar machine. This includes independent brush paths and compensated current paths. Common sense tells us that geometric similarity will provide more accurate data, but past experience indicates that geometric similarity is a requirement and not just a desire.

The independent brush paths insure that any differences in the films of the positive and negative brushes will be maintained. The positive and negative voltage drops are generally different, and arcing will often occur on one side only. This indicates that there may be differences in the establishment or breakdown of the films. These differences cannot be maintained if positive and negative brushes ride in the same track.

The compensated current paths are required to minimize stray magnetic fields that can perturb contact drop and friction measurements. For example, data from a test rig without current compensation indicated an apparent relationship between the friction coefficient and the current density. However, no conclusion could be drawn from this because eddy currents were induced as the rotor rotated through the stray fields, thereby artificially loading the rotor. These eddy currents can also cause voltage drops that are mistakenly attributed to the rubbing interface.

Due to the large current densities required, and in order to keep the size of the experimental current source to a manageable level, it is desirable to fabricate a small, high speed test rig.

The traditional methods of power transmission, high speed gearing or belting, were found to require multiple stages of rotational

speed increases. They both resulted in systems that were either overly complicated, very expensive, or required large power input just to feed the associated losses, such as bearing loss or loss due to belt slippage. High speed belting was inappropriate since the centrifugal force acting on the belt was enough to lift the belt from the rotor surface.

An attractive alternative was found in an "inside-out," 2-pole, solid armature induction motor. The 2-pole motor will be driven by a power supply capable of providing 3-phase power at up to 400 Hz.

It should be noted that this test rig is not actively cooled. Testing will be limited in duration by the observed temperature rise of the motor field and the rotor conductor.

2.2.2 High Speed Test Rig Electrical Design

An "inside-out" solid steel rotor polyphase induction motor was selected for the current collector test rig. By locating the solid steel rotor at the outer periphery of the motor and by making the appropriate choice of rotor diameter and motor poles and frequency, the test collector rings may be operated at the desired surface speed while minimizing the volume of collector ring material required. The stator winding, while concentrated in a smaller volume than in the conventional induction motor configuration, receives adequate cooling for intermittent operation of the test rig. (The stator winding has integral thermocouples for thermal monitoring.) Motor line leads and thermocouple leads are brought out through the hollow stationary "shaft" of the motor. The motor specifications are shown in Table 2.2.

Table 2.2 - HIGH SPEED TEST RIG MOTOR SPECIFICATIONS

Nominal rated hp	15
Duty	Intermittent
Enclosure & cooling	Drip-proof/self-ventilated
Poles	2
Phase	3
Nominal frequency (hertz)	400
Volts (line-line)	350
Insulation	Class H (no silicone materials used to avoid contact contamination)
Speed @ rated load & 400 Hz	22000 rpm (approximate)
Ampere @ rated load & 400 Hz	35 (approximate)

2.2.3 High Speed Test Rig Mechanical Design

The rotor consists of a low carbon cold-drawn steel tube upon which is shrunk a chrome copper tube. The steel tube acts as the armature for the "inside-out" induction motor driven by the field winding located inside the tube. The copper cylinder becomes the high speed current collection surface. The rotor cylinder is completed by endplates which accept high quality, shielded 25-mm ball bearings capable of operation at speeds in excess of 24,000 rpm. The endplates are made of 7075-T651 aluminum. In addition, these endplates are trepanned. The combination of the material and the geometry allows each endplate to expand at the outside with the copper while still retaining contact at outer race of the bearing. The completed rotor is shown in Figure 2.6.

The rotor containment consists of a 1.25-in thick steel cylinder. This cylinder is split at the axial centerline of the rotor to accommodate two copper plates that act as the current conductors. This geometry results in a compensated current path much like those found in actual homopolar machines. This eliminates any stray magnetic fields that do not accurately reflect the working environment of the collectors. A cross-sectional assembly view of the test rig is shown in Figure 2.7.

Each brush will be independently actuated with a double acting miniature air cylinder. The brush will slide in grooves machined in two mating copper rings. The current will be shunted to the brushes through finger contacts. Other options involving fixed shunt contact were discounted because of space restrictions and brush movement requirements. This arrangement allows for over 0.25-in of brush wear before sufficient contact between the brush and rotor surface would be lost.

The finger contacts consist of 0.005-in thick beryllium copper plated with 0.002 to 0.005 in of silver. They are soldered to one of the copper rings. The back of the brush ring is provided with a channel for cooling water to flow. This channel was placed as close to the finger contacts as reasonably possible for maximum cooling. The finished assembly with one front ring removed is shown in Figure 2.8.

Figure 2.6 — Completed rotor for high speed test rig.

Figure 2.7 — High speed test rig schematic.

Figure 2.8 — Completed brush rings.

The structure is entirely enclosed so that the environment can be controlled.

2.2.4 Data Acquisition System

The computer-aided instrumentation will be designed to service two sets of brushes, the high current power supply, the motor power supply, and a tachometer in the following way:

The three-phase power supply will be instrumented to monitor input voltage, current, and power to the drive motor. The speed will also be monitored with an optical tachometer. These measurements will provide the basis for an estimate of the friction coefficient.

Each brush ring will be fitted with several small auxiliary brushes to monitor the potential of the rotor. They will be situated about 0.5 in. outside of the main brush path. Combining these measurements with measurements of the potential of the brushes will provide the contact drop at the brush-rotor interface.

Thermocouples will be mounted inside several of the brushes and at several places on the brush ring. In addition, temperature at the bearings, inside the stator winding, at the inlet and outlet of the brush ring cooling channel, and at points within the controlled atmosphere, will be monitored.

The computerized data acquisition system consists of the following:

- IBM PC-AT personal computer with 30-MB hard drive, one 1.2-MB floppy drive, and one 360-KB drive.
- Intel Above Board memory expansion unit, to provide RAM disk with 1.2-MB capacity.
- Metrabyte DAS-8 analog input board, with 4000 channels/s capability.
- Metrabyte EXP-16 expansion board for 16 voltage measurements.
- Metrabyte EXP-16 expansion board for measuring 16 thermocouples.
- LABTECH NOTEBOOK software to drive the data acquisition system.

A picture of the data acquisition system is shown in Figure 2.9.

The LABTECH NOTEBOOK software allows for various sampling rates for each channel. In the interest of speed and memory, the voltage channels will be sampled between 10 and 30 times a second, whereas the thermocouple channels will be sampled one time a second.

2.2.5 High Speed Test Rig Status

Initial attempts at soldering the finger contacts resulted in annealing of the beryllium copper within the contacts. These initial attempts were made with a pure tin solder that melted at 250°C. Samples placed in an oven at 250°C needed at least an hour to achieve the correct temperature, during which time the finger contacts annealed.

Tests were conducted to determine to what temperature and for how long the finger contacts could be heated. After several tests, it was determined that the finger contacts would retain sufficient temper if the temperature were held below 200°C for no more than an hour.

The solder was changed to a mixture of 50% tin and 50% lead. This solder has a melting temperature of between 180°C and 190°C. A mockup was made to test methods of controlling the temperature at the contacts. It was determined that if a large copper plate was heated to 250°C and then the brush ring was placed onto this block, the temperature could be held to less than 200°C. Both rings have been completed and inspections show that the finger contacts did retain sufficient temper.

The test rig has been assembled without the brush gear in place. This was done to insure smooth operation of the test stand prior to expending the effort to assemble the brush gear. Also, this represented the least risk to the test rig if a failure were to occur. Figure 2.10 shows this assembly.

The rotor was run to 80% of full speed, or 18,000 rpm (300 Hz). Critical speeds were found at 5,400 rpm (90 Hz), 9,000 (150 Hz), and 18,000 rpm (300 Hz). The first two did not present any problems. The rotor was easily run through these speeds without significant vibration. However, the 18,000 rpm (80% of running speed) resonance did present a

Figure 2.9 — IBM PC driven data acquisition system.

Figure 2.10 — Assembled test rig.

problem. In addition, there appeared to be a resonance at 21,900 rpm (365 Hz). Running the rotor between 18,000 rpm and 21,900 rpm was not attempted because it was considered too risky.

The rotor support structure was fitted with accelerometers in order to better understand the system motion. It is believed that the vibration at 18,000 rpm was a combination of the rotor, stator, and rotor support pieces shaking against each other. A simple spring-mass analysis done during the design phase predicted this resonance at about 16,200 rpm (270 Hz). This analysis was performed again after the rotor was run, and by reducing the weight of the rotor support pieces, the frequency of this resonance increased. By changing the material of these pieces from steel to G10 composite, this 18,000 rpm resonance was moved to a about 24,000 rpm (400 Hz). The 21,900 rpm resonance was no longer apparent. For this particular system, the 18,000 rpm and the 24,000 rpm resonances will approach each other as the weights of the support pieces decrease.

This test rig was run to 21,000 rpm (350 Hz - approximately full speed). After about a minute of operation, one of the bearings slipped in its housing. It is believed that this failure occurred because the bearing and rotating housing got too warm. The aluminum then grew away from the steel bearing race due to a combination of rotational stresses and thermal expansion differences.

We originally believed that the reasons for failure were as follows:

- The housing that grew away from the bearing was on the exhaust side of the rotor and no air passed near the bearings.
- Replacing the steel rotor support pieces with the composite pieces significantly reduced the amount of heat that could be removed from the bearing area.
- The bearings were too heavily preloaded causing the ball to ride up high on the race.

To remedy these problems:

- Air intake holes were put in the housing that grew away from its bearing.
- A water-cooled jacket was fit into the stator support pieces.
- The bearing preload was reduced to half of its original value.
- A titanium sleeve was shrunk into the trepanned slot to keep the inside diameter of the housing from expanding.

After these changes were effected and the rotor was rebalanced, it was determined that the copper had actually yielded. In addition, it is believed that the copper slipped at its interface with the steel tube. This combination resulted in the use of all of the bearing travel. The bearings were effectively locked in place with an excessive axial load. This is believed to be the main reason for the failure.

Despite the fact that the copper would have to be machined again, we decided to rerun the rotor. One reason for running again was to insure that the copper would not change further. The other reason was to gauge the benefits provided by the changes to the rotor.

Approximately 10 runs at various speeds were made. Temperatures inside the stator and near the bearings were monitored. Most of the runs lasted long enough for the temperatures to reach our predetermined limits. This is important because excessive temperature can cause failure, particularly when coupled with high rotational speeds.

Most of the runs were to 17,100 rpm (285 Hz) or better. All of them lasted between 10 and 15 minutes. The fastest run was to 20,400 rpm (340 Hz, 95% of full speed) for 12 minutes. The duration of these runs indicates that the changes made were effective. Though no heat was introduced at the collector surface, tests lasting 30 to 300 seconds should be feasible.

After each of these runs, a runout measurement was made on the outside diameter of the copper. The erratic nature of these runout values indicated an inconsistency in the measurement technique employed. For the last two runs, the measurement technique was changed to increase

the consistency between readings. These measurements showed no discernible changes from run to run. We believe the copper has stabilized. Consequently, it has been machined and rebalanced.

We plan to operate the test rig to 85% of its rated speed. This corresponds to a 170 m/s running speed. This is considered a safe speed to run the test rig, and it is believed that the knowledge gained at this speed will be sufficient to understand the high speed behavior. This speed is well above any speed previously achieved for continuous collector duty. After testing at this speed is completed, the rig may be run up to 200 m/s for additional tests. These would not be dangerous tests, but additional yielding might affect the results.

It should be noted that the complications encountered here were primarily a result of the large interference fit between the copper and the steel tube. This is only a factor on our test rig and not on an actual homopolar machine. A homopolar machine could be built and operated at 200 m/s rubbing speed with this material.

2.3 HPG LOSS ANALYSIS

2.3.1 Summary

To date, no adequate analytical description of the distribution of rotor currents in the rotor conductor of a homopolar generator is available. Accurate knowledge of this phenomenon is essential to the design of homopolar power supplies operating at high power levels.

In order to define the governing physical phenomenon, an analytical model of the current collector-rotor conductor region has been constructed.

The model is based on a cylindrical conductor with prescribed boundary conditions occurring along its outside surface. The boundary conditions imposed are that current is injected radially into the conductor at prescribed areas.

Figure 2.11 shows the locations where radial current is injected into the rotor. For our test case, this boundary condition is expressed as a double Fourier's series each containing 12 terms. A surface plot depicting these conditions is shown as Figure 2.12, where elevations

Figure 2.11 — Boundary conditions on surface are expressed as limits on radial currents over outer surface and condition of no axial currents from ends.

Figure 2.12 — Surface plot showing radial current injected at cylindrical surface.

above the surface represents current out of the rotor.

Physical parameters were modeled after those of the 50-hp demonstration homopolar motor designed for proof of concept in homopolar drive of tracked vehicles. These motors operate at 80-m/s rubbing speed with a 5-in (12.7 cm) diameter rotor at 5-V terminal potential. This was chosen because we have built and tested these devices, and we know their total losses to a high degree of confidence.

The model was run for a 5-in (12.7 cm) diameter copper rotor conductor operating at a peripheral speed of 100 m/s. Sixteen brushes were used on each end of the rotor.

Figure 2.11 represents the input boundary conditions imposed upon the model. A strong skin effect of time-varying surface currents is developed. Details of the analysis can be found in Appendix A.

This model will allow assessment of current collector designs, with the result of loss minimization.

It is felt that the identification and quantification of this phenomenon represents a significant advance in the state-of-the-art. A paper summarizing this work was presented to the International Current Collection Conference held in Austin, Texas on November 17 and 18, 1987. This paper is presented in Appendix A.

2.3.2 Status

No significant work was performed in this area during this reporting period. Further work, other than minor refinements, will require the use of the high speed test stand for verification.

2.3.3 Rotor Heating

The principles of this theory are currently being applied to the conduction of heat in the rotor. The formulation is very similar with the heat flow being analogous to current.

2.4 HOMOPOLAR OPTIMIZATION STUDIES

The thrust of the optimization studies was to develop the design criteria for attaining the maximum power density homopolar machine. The

major goal of the program was to achieve power densities in excess of 80 kW/kg for multimegawatt, continuous duty homopolar machines operating in the 50 to 400 V range. In this context power density is defined as the ratio of the output power, which the machine can supply on a continuous basis, to the total mass of the homopolar machine. It should be noted that some authors use power density as the peak instantaneous power divided by machine mass which can be very misleading.

During the course of the studies the operational parameter space for achieving high power densities was developed for both iron-core and air-core machine types. A pre-existing optimization code was used for the iron-core machine evaluations and changes were then made to that code to incorporate air-core designs.

Iron-core refers to those machines which utilize a high magnetic permeability material to define a low reluctance path for the flux which is utilized in the homopolar machine to produce voltage. Air-core refers to those machines which do not utilize a magnetic material to provide such a low reluctance magnetic flux path.

The initial studies confirmed that machines based on iron-core technology would not be able to achieve the high power densities and efficiencies required for this program. Furthermore, any advancements which could be applied to iron-core machines could equally well be applied to air-core machines which have the potential for higher power densities.

Although iron-core machines are inadequate for the SDI Mission they can achieve quite impressive power densities and efficiencies when compared to conventional rotating machine technology. Also at power levels less than 2-MW iron-core technology is competitive with air-core designs in terms of power density and efficiency.

The analysis of air-core homopolar machines was greatly facilitated by the development of a new and novel method for their design and optimization. In this new method, the machine performance equations are formulated in terms of the magnetic vector potential, A . This is in contrast to the conventional approach of formulating the performance equations in terms of the average magnetic flux density in

the active region, B_a . Formulating the equations in terms of the magnetic vector potential leads to a straightforward optimization procedure for maximizing machine power density.

Initially a simplistic model was used to verify the new method. This model was then expanded to include all of the major design and construction features of an air-core homopolar machine. A series of parametric studies was then undertaken to establish the functional design requirements and to develop the operational parameter space for air-core homopolar machines.

These studies identified the technologies necessary to achieve power densities in excess of 80 kW/kg. Through these studies it was learned that these high power densities can be achieved using demonstrated cryogenic thermal management techniques, materials, and sliding contact speeds.

A surprising and extremely important finding was that the high power density requirement could be met at magnetic field strengths below 2.0 T in the active region and less than 2.3 T anywhere in the machine. This means that the current collector would operate in the same fields as demonstrated in conventional iron-core machines. This greatly simplifies the design of current collector in terms of Lorentz forces and circulating currents.

Another important finding made obvious by the vector potential approach is that the operating flux density in the active region, B_a , is not an arbitrarily selectable quantity. It is determined by the selection of the rotor diameter, the thermo-physical properties of the materials, and the thermal management scheme employed in the design.

The design and optimization studies has significantly advanced the state-of-the-art in the design and physical understanding of the operation of air-core homopolar machines. A detailed discussion of this work is presented in Appendix B.

3. PRIOR ACCOMPLISHMENTS

The accomplishments cited in this section were reported on in the previous annual report. No further work has been performed in these areas.

3.1 HOMOPOLAR MACHINE APPLICATION STUDY

3.1.1 Background

The objective of the Machine Application Study was to define a homopolar power supply which was suitable for the SDI mission requiring hundreds of megawatts of continuous dc power in the 50- to 400-V range. The major operational requirement of the system was that the power density exceed 80 kW/kg.

To define the operational and functional design requirements for the current collection system, a baseline homopolar machine concept (which met the operational goals of the SDI mission) was selected. In particular, the selection of a baseline machine quantified the cooling fluids, rubbing speed, current densities, and the thermal and electromagnetic environments for the current collection system.

Two basic homopolar machine concepts were evaluated as part of the selection of a baseline machine. These were the iron-core and air-core machine arrangements. All concepts employed drum type construction (refer to Figure 3.1) as previous studies have shown these to have the highest power density and voltage generating capability.

Previous studies for high voltage pulsed power systems have shown that air-core machines will have higher power densities than iron-core designs of the same rating. This is because the structural members in air-core machines need only be designed for mechanical strength and not to carry magnetic flux. The savings in the weight of the structural materials more than offsets the added weight of the

Figure 3.1 — Ideal drum type homopolar motor.

larger field coil. However, these studies dealt with very high voltage (thousands of volts) homopolar machines which required magnetic field strengths on the order of 4 to 8 T. At these high magnetic field strengths there is no advantage to using ferromagnetic materials.

Because low voltage (50 to several hundred volts), high power density, high efficiency machines have not been previously investigated, a benchmark assessment of iron-core technology was included as part of the application studies. The results of these studies are presented as Appendix C.

3.1.2 Conclusions and Recommendations

The work completed to date suggests that rather impressive power densities are obtainable using iron-core technology. However, given the present limits on brush current density and rotor tip speed, the maximum obtainable power density approaches 30 kW/kg at an efficiency of 96%, which is short of the SDI requirements. Further increases in energy density can only be achieved by dramatically increasing the rotor tip speed and/or brush current density. It does not appear that sufficient breakthroughs in this technology can be made to make it viable for the low voltage (50 to 100 V) SDI application.

Iron-core homopolar machines are not suited for the SDI mission requiring multimegawatt, low dc voltage, continuous operation, for the following reasons:

- 1) To meet the SDI mission requirements the machine will require cryogenic cooling. The physical properties of magnetic steels are significantly degraded at these temperatures, and interfaces between the magnetic steel rotor and stator with the cryogenic components would require significant developments in the state of the art.
- 2) To achieve the power density increases necessary to meet SDI goals, the brush current density and rotor tip speed limits will have to be significantly increased. Since any developments in this area apply equally well to air-core

technology, which already has significantly higher power densities, this work should be directed toward those machines as the benefits will be greater.

- 3) The minimum efficiency requirement keeps the cooling requirement in line with fuel consumption. To achieve this minimum efficiency means that the friction coefficient and contact resistance will have to be significantly lowered. It also means that the homopolar machine will have to operate at less than the maximum power density point. The higher tip speed is also working against the efficiency requirement. Again, since an air-core technology-based machine has an inherent advantage in terms of power density it can more readily be operated "off peak" without degrading performance below acceptable levels.

Previous work has indicated that air-core homopolar machines are viable for the medium voltage (several thousand volts) multimegawatt pulsed duty SDI application. The application study conducted for this program shows that they are also applicable for the low voltage continuous duty application as well. The major problem in the low voltage application will be to maintain a high power density at high efficiency. The critical factors are again rotor tip speed, contact resistance, brush friction, and operating current density.

The most significant findings of the study are:

- 1) The average magnetic field in the active region of air-core homopolar machines (operating at maximum power density) is not an arbitrary quantity. It depends on the rotor diameter, the material thermal properties, and cooling technique employed in the design.
- 2) Power densities of 80 kW/kg can be achieved with average flux densities in the active region of about 1 T and peak values anywhere in the magnetic circuit under 2.3 T.

It is recommended that the application studies be continued to determine the interrelationships between the various parameters which define the current collection system (i.e., trade off between friction coefficient, cooling scheme, operating current density). Further studies of alternate design concepts such as the inside out machine (field coil on the inside of the rotor), the impact of dimensional clearances, and field coil shaping should also be undertaken.

3.2 ROTOR CONDUCTOR CONFIGURATION

3.2.1 Rotor Stress

The successful design of the rotor conductor must address the following sources of stress acting upon the rotor conductor;

- Thermal Loading
- Dynamic Loading
- Fluid Pressure Loading

The rotor surface of the current collection slipping is expected to absorb large quantities of heat (estimated as $\approx 4.5 \text{ MW/m}^2$). Conduction of this heat into the interior of the rotor conductor will establish a temperature gradient, and associated thermal stress. The temperature gradient is set by the heat flux and the thermal conductivity of the material, while the overall temperature difference is dependent upon the length through which heat must be conducted. The developed stress is proportional to the developed temperature difference.

The necessity of limiting thermal stress under the anticipated heat loading suggests that the typical length of the heat conduction path be limited to a dimension of 0.9 cm (0.35 in) or less.

3.2.2 Heat Transfer

An efficient method of heat removal has been identified. Pool boiling occurring under the influence of large centripetal accelerations is believed to be adequate to remove the heat flux required. Nucleate

boiling heat transfer exceeds natural convection. When nucleate boiling is established in a strong centripetal field, a large increase in the heat transfer coefficient is attained. This is due to establishment of strong convection currents driven by buoyant forces acting on the fluid.

Heat transfer coefficients are expected to be sufficiently large to keep the wall temperature at the inside radius of the current collector cylinder quenched nearly to the saturation temperature of the coolant used.

This method of cooling is well matched to an air-cored homopolar machine. It relies upon the latent heat of vaporization, which is an efficient use of the cryogenic fluid. The spent coolant is ejected from the rotating pool of coolant towards the axis of the rotor, where it is collected and ejected from the machine.

Operation of a current collector slip-ring at 200 m/s suggests a cylindrical tube construction. The centripetal loading of the current collector will create hoop tension stress of a moderately high value. It is believed that the current collector and armature conductor can be made as a self supporting tube of a high strength, high electrical conductivity material such as chrome copper.

If the armature conductor is not made as a cylinder, alternative configurations must provide significant supporting structure to contain the conductor. It is believed that this results in an unnecessarily complicated, heavy design.

A final source of significant loading to the rotor conductor has been identified as the fluid pressure loading of the coolant fluid circulating in the high centripetal field caused by high speed rotation.

Operation utilizing liquid hydrogen as the coolant results in relatively small, manageable stress levels.

The low temperature material properties of candidate conductor materials all tend to be superior to room temperature values. The design criterion being followed is to limit stress to a maximum value of $2/3$ the room temperature yield.

3.3 SHUNT-BRUSH DIFFUSION BOND

Inherent in the construction of most current collection systems is the utilization of "brushes." These brushes are typically made of graphite formed with various other materials. In the case of high powered homopolars, these brushes are typically metal graphite (greater than 90% metal by weight). The metals usually employed are either copper or silver. These brush materials are normally formed by processes similar to those used in powder metallurgy.

A problem arises when a shunt must be attached to such a composite brush. The graphite present in the material does not allow welding or brazing of copper shunts to the brush, because volatile products are driven off. Typical shunt attachments are executed using special tamping techniques, riveting, or soldering. All these techniques appear inadequate to meet the demands imposed by continuous duty, high powered homopolar power supplies.

In order to identify a means of forming a "good" metallurgical bond between a copper "shunt" and a conventional "brush," some sample joints were attempted using a hot isothermal process. Under this process the brush material and copper test piece were "canned" inside a small container. Pressure and temperature were applied and a good diffusion bond is found to have occurred as shown in micrographs of the test pieces shown as Figures 3.2 and 3.3.

This addresses a present weakness in current collection design, since many brush designs are limited by the temperature limitations imposed by the shunt attachment, rather than the more basic temperature limitations imposed by the current collection rubbing interface.

A patent application has been filed for this process.

Figure 3.2 — Micrograph of the interface between the #359 Cu-C and copper.

Copper

Figure 3.3 — Micrograph of the interface between the #933 Cu-C and copper.

4. SCHEDULE/STATUS

Work on the high speed test rig is behind schedule due in part to the two six-month funding stoppages, and in part to complications encountered during initial running tests. Current in house funding will run out in March 1990. Completion of the program on time will require receipt of the last funding increment prior to March.

Figure 4.1 — Current collection program schedule.

5. FUTURE EFFORTS

5.1 TEST PROGRAM

A series of tests will be performed on the high speed test rig. This test rig will allow thermal characterization of a current collection system utilizing sufficient rotor coverage to attain meaningful results. This represents a very powerful and unique test device, since the only present test devices accomplishing this goal are full scale, high powered homopolars.

5.2 DETAILED LOSS ANALYSIS

The model of the current-induced losses will be further refined to accommodate analysis of smaller areas of current conduction. This will enable characterization of expected losses occurring at current collection sites with areas of contact predicted by the present theory.

The data gathered from the high speed test rig should provide some level of verification of this analysis.

5.3 PROTOTYPE CURRENT COLLECTION SYSTEM

The prototype current collection system will be incorporated in the cryogenic test rig. The construction of the high speed test rig has cost more than expected. This is due to the two six-month funding delays and subsequent work stoppages, and the problems encountered with the high speed test rig. The strategy of modifying the cryogenic test rig will allow the program to be completed with the contracted funding.

This design will involve several brushes riding on the cooled rotor, probably shunted with finger contacts. A larger power source will be employed so that a current density of 2 kA/cm^2 can be achieved.

This collector system will be designed to operate at 2 kA/cm^2 , at 200 m/s and with 20% coverage. However, our test rig will not be able to achieve a 200 m/s rubbing speed or 20% coverage. This test rig will achieve the project goal of operating a cryogenically cooled prototype system under heavy thermal loads.

This rig will provide an excellent basis for the characterization of the rotor and brush heating process. This current density represents a significant heat load on this system, and much can be learned about high powered current collection with cryogenic cooling.

6. ACKNOWLEDGMENTS

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THE OPTIMIZATION AND DESIGN OF "AIR-CORE" HOMOPOLAR MACHINES

Dennis Pavlik
Westinghouse Electric Corporation
Research and Development Center
1310 Beulah Road
Pittsburgh, PA 15235

Abstract - This paper presents a novel method for the design and optimization of "air core" homopolar machines. In this new method, the machine performance equations are formulated in terms of the magnetic vector potential, A . This is in contrast to the conventional approach of formulating the performance equations in terms of the average magnetic flux density in the active region, B_a . Formulating the equations in terms of the magnetic vector potential leads to a straight-forward optimization procedure for maximizing machine power density. A simplistic model is used to illustrate the new formulation. Next, a more complete model is developed and used to establish the functional parameter space for "air core" homopolar machines and identify the technologies needed to improve their performance in terms of maximizing power density. Power densities in excess of 80 kW/kg are obtainable with conventional technology and at average magnetic field intensities of less than 2 T.

INTRODUCTION

Homopolar machines have seen renewed interest in the last decade as sources of high current pulsed power for electromagnetic launchers (EML), resistance welders, metal compaction devices, and fusion research.

The Department of Energy (DOE) is currently funding a program to define the thermo-physical operating requirements for a homopolar power supply which will be capable of continuous duty multimewatt operation in the 50- to 500-V range at power densities in excess of 80 kW/kg.

A systematic design and optimization technique along with a baseline design concept are necessary to properly determine the functional and operating requirements for the current collection and thermal management systems.

Such a systematic optimization approach has been developed for "iron core" machines. It is based on the average or working magnetic flux density, B_a , and the "Blv" equation for voltage generation [1,2]. In these machines the arrangement (disk, drum, truncated drum, spool, etc.) and B_a define the general physical arrangement and the basic proportions of the machine. The physical dimensions and performance of the machine are uniquely defined with the selection of the voltage and output power, and the thermo-physical limitations on the structure (i.e., collector sliding velocity, operating current densities in the collector, field coil(s), and main conductor).

In "air core" machines the working magnetic flux density is not the defining parameter as it is in "iron core" designs nor does its definition lead to a unique machine configuration. In these machines, it is the rotor diameter which is the key independent physical parameter determining the magnetic circuit and operating flux density in addition to establishing the basic machine dimensions, weight, rpm (for a fixed tip speed), and power density.

Studies [3,4] have shown that an "air core" based machine would be required to achieve power densities in excess of 80 kW/kg. The work reported in this paper is the result of an effort to define a

systematic design optimization approach for "air core" homopolar machines.

SIMPLIFIED MODEL

A simplified homopolar machine configuration is shown in Figure 1. This model comprises a set of circular filamentary conductors, a rotor surface, and two infinitesimally-wide brushes. The governing equation for the voltage generated between the two brushes is given by (1). This degenerates into the well known "Blv" equation if B is interpreted as the average flux density between brush pairs.

$$E = \frac{\phi \omega}{2\pi} = \frac{\phi \text{ vel}}{2\pi R_{\text{rot}}} \quad (1)$$

where: E The voltage generated between the brushes.
 ϕ The flux crossing the cylindrical surface between brushes.
 R_{rot} Radius of the rotor.
 ω Angular velocity of the rotor.
 vel Rotor tip speed.

Although Figure 1 implies that the rotor is a cylindrical surface, (1) is valid for any rotor configuration as long as ϕ is interpreted as the flux between the brush pairs.

Fig. 1. Simplified machine configuration.

The remainder of this development is for drum machine topologies. This does not mean that the rotor must be a drum. Drum topology in this work means that the brush pairs are at the same radius. The rotor surface can be irregularly shaped between brush pairs as long as it is continuously conducting. For any drum topology, the flux between brushes can be put in terms of the magnetic vector potential as given by (2).

Since circular current loops have been assumed, the vector potential, A , has only a component in the peripheral direction. Therefore, when A is used for the vector potential it is recognised that this refers to the peripheral component.

$$\phi = (A_1 - A_2) \cdot 2 \cdot \pi \cdot R_{\text{rot}} \quad (2)$$

The average flux density, B_a , between the two

brushes is given by (3)

$$B_a = \phi / (2 \pi R_{rot} L) = (A_1 - A_2) / L \quad (3)$$

where: A_1 The vector potential directly under the first brush at $s = 0$.
 A_2 The vector potential directly under the second brush at $s = L$.

Combining (1) and (2) gives (4) for the voltage, E , generated between brush pairs.

$$E = vel \cdot (A_1 - A_2) \quad (4)$$

E_0 is defined as the voltage corresponding to an infinite brush spacing where $A_2 = 0$. This is the maximum theoretical voltage which can be generated by a particular coil at the specified rotor radius.

Neglecting losses, the power out of the homopolar machine is $E \cdot I$, where I is the load current through the machine. For the idealised model of Figure 1, the field coil is a filamentary conductor and has no volume or mass. Therefore, the power density, PDEN, is just the power out divided by the rotor volume times a factor to convert rotor volume to mass (5).

$$PDEN = \frac{E \cdot I}{G \cdot \pi \cdot R_{rot}^2 L} = \frac{vel \cdot (A_1 - A_2) \cdot I}{G \cdot \pi \cdot R_{rot}^2 L} \quad (5)$$

where: G A factor to convert rotor volume to mass
 L Spacing between brushes (equal to rotor length in the simple model).

As the brush spacing is increased, the voltage approaches the limit E_0 as L tends to infinity. However, the rotor volume increases without limit and therefore the power density approaches zero.

For small L the output power decreases faster than the volume of the rotor decreases and therefore the power density goes to zero as L becomes small.

To differentiate (5) it is necessary to develop the magnetic vector potential as a function of s using a formulation for a filamentary current loop. For this work the magnetic field equations were formulated in terms of complete elliptic integrals [5] as given by (6).

$$A(r, z) = \frac{\mu_0 I_f}{r k} \left| \frac{R_c}{R_{rot}} \right|^{0.5} \{ (1 - 0.5 k^2) K - E \} \quad (6)$$

where: μ_0 Permeability of free space.
 I_f Current in the filamentary coil.
 R_c Radius of the magnetic centroid of the current loop.
 k Modulus of the elliptic integral, which is $\text{SQRT} (4 R_{rot} R_c / [(R_{rot} + R_c)^2 + z^2])$.
 K & E Are the complete elliptic integrals of the first and second kind.

Substituting (6) into (5) and differentiating gives (7). Setting (7) equal to 0 and solving for z gives the brush spacing at which the maximum power density is achieved in the idealised machine.

$$\frac{\partial PDEN}{\partial z} = \frac{\mu_0 I_f}{G \pi R_{rot}^2} \left\{ \frac{1}{2} \{ A(s) - A(0) \} - \frac{k}{4 R_{rot} R_c} \left\{ K - E \frac{(1 - k^2/2)}{(1 - k^2)} \right\} \right\} \quad (7)$$

Figure 2 is a plot of normalized power density, at fixed excitation, for different ratios of rotor radius to coil radius. Observe that the power density increases as the ratio of rotor radius to field coil mean radius is increased.

Both B_a and PDEN are proportional to the difference in vector potential between brushes divided by the brush spacing L . Both will be maximised at the same brush spacing.

Fig. 2. Normalised power density vs. brush spacing.

The internal machine voltage at the brush spacing which gives maximum power density varies between 30% and 54% of E_0 depending on rotor radius. However, by selecting a brush spacing such that the generated voltage is equal to half of E_0 , the power density is within 3% of the maximum value for all practical rotor dimensions.

Figure 3 is a plot of the ratio of the PDEN at the half voltage spacing to the maximum PDEN given by (7) for different rotor radii. Observe that the power density at half voltage versus maximum power density does not begin to fall off until the ratio of rotor to coil radius is greater than 0.8. It is still within 10% of maximum for $R_{rot}/R_c < 0.9$.

The preceding development was for a machine configuration with a single excitation coil, as shown in Figure 1. When another coil is added over the second brush, as is typical with drum configurations, it can readily be shown by symmetry that the generated voltage, power, power density, and B_a are twice the single coil values.

Fig. 3. Plot of the ratio of power density at half voltage to maximum power density.

The idealised model has established that: 1) There is an optimum spacing between brushes for maximum power density, and 2) Increasing the ratio of the rotor diameter to the mean field coil diameter results in increased power density.

ESTABLISHING DESIGN RULES

The simplified model provides the relationship between the electromagnetic and physical constraints of the 'air core' homopolar machines in much the same way that the equal flux density criterion [1] does for 'iron core' machines.

A set of screening studies was conducted to establish design rules for the various machine components. This was done in order to limit the range of topological variations to be explored in the parametric studies.

Field Coil

The ampere-turn requirements for any voltage and tip speed can be found from (4) and (6) if the rotor and coil radii are specified. The coil radius is a function of the rotor radius, clearance between rotor and coil for the current collector, current density at which the coil can be operated, and its cross sectional shape (square, round, rectangular, etc.)

A rectangular coil cross section (with the long dimension along the axis) was found to maximise power density. The screening studies indicated that a ratio of coil axial length to radial build of around 2 to 1 seemed to provide the best results, although some further investigation is necessary.

The reasoning for selecting this aspect ratio follows:

- 1) An axially-long, radially-thin coil shape maximises the ratio of rotor radius to that of the magnetic centroid of the field coil defined in (8) as

$$c = R_{\text{rot}} / R_c \quad (8)$$

Since the useful flux varies as an elliptic function of c and a linear function of radial thickness, increasing the level of excitation by adding radial height decreases the useful flux and hence power density.

- 2) An axially-long, radially-thin coil has the minimum volume for a fixed level of excitation and current density.

- 3) The field coil must also be used to shape the flux profile in the current collection region to minimise the radial flux through the sliding contacts [6], which implies an axially-long coil.

- 4) There is a maximum length coil beyond which the power density is also decreased. As the coil length is increased, the maximum flux through the coil reaches a limit governed by the equation for an infinitely-long solenoid [7]. Increases beyond this length result in no gain in total flux through the coil and hence an upper limit on the voltage which can be generated in a homopolar device.

- 5) Too long a coil forces the brushes to be spaced farther apart than the optimum spacing given by (6) so that at some point further increases in coil length cause a decrease in power density.

Rotor Size

The minimum rotor size is set by how much heat is generated by the friction and contact losses in the current collector, the ohmic loss in the conductor due to load current, the area needed to provide for coolant flow, and structural integrity requirements.

The volume of the rotor increases between a square and cubic function of rotor radius because the sliding contact spacing L increases as some function of rotor radius and therefore so does the machine length.

For a fixed level of excitation, the field coil volume increases linearly as the rotor radius. For a fixed voltage (constant tip speed) the field coil volume varies roughly as the inverse function of rotor radius.

As the rotor radius increases, so does the useful flux and current handling capability. Therefore, the output power also increases with increasing rotor radius.

The design rule established by the screening studies was that the rotor radius be sized based on its thermo-physical limitations.

General

The output power and power density increase without limit as the field coil current density, rotor current density, and operating current density within the sliding contacts are increased. The same holds true for increases in the allowable rotor tip speed.

PARAMETRIC STUDIES

The objectives of the parametric studies were to: 1) Verify the design criteria established by the simplified model, 2) Establish the performance criteria and design requirements needed to achieve power densities in excess of 80 kW/kg, 3) Investigate different mechanical arrangements in an effort to improve power density.

The simplified model was incorporated into a more comprehensive analytical model which considered the mass of the major structural components. A number of verification trials were then completed to verify the optimisation criteria, in particular the 50% E_0 rule for maximum power density. These studies confirmed that the rules established by the simplified model were valid for realistic machine configurations.

A number of studies were conducted for different ranges of rotor tip speed, current density limits, and design concepts. These studies are in agreement with previous studies [3,4,8] which predicted that an 'air core' design with cryogenic cooling would be required to achieve the high power density goal of this program.

Figure 4 shows a typical power density (PDEN) versus voltage curve for a 50-MW homopolar machine using the design constraints of Table 1. Observe that, just as with iron core designs, there is a voltage where PDEN peaks. For the 50-MW machine, PDEN peaks at 80 kW/kg with an internal voltage of 51 volts.

The average radial flux density between the brush pairs was 1.08 T and the peak flux density was within the field coil and was about 2.3 T for the peak power density operating point.

Table 1. Thermo-physical Design Constraints

PARAMETER	VALUE	UNITS
Field Coil Cooling	Liq. H ₂	Hyperconducting
Rotor Cooling	Liq. H ₂	Hyperconducting
Rotor Tip Speed	200.0	m/s
Brush Current Den.	20.0	kA/cm ²
Field Coil Current Den.	77.5	kA/cm ²
Rotor Conductor Current Den.	77.5	kA/cm ²
Brush Resistance (cm ²)	0.001	μΩ-cm
Friction Coefficient	0.1	---
Rotor Cooling Factor	1.484	kW/cm ²

The curve shown in Figure 4 was constructed by assuming a fixed output power and then sweeping through a number of different voltages (for fixed tip speed). The fixed power and terminal voltage established the load current which then established the rotor size, conductor dimensions, and brush box length.

The rotor size, clearances for the current collector, and (4) and (6) establish the field coil excitation requirements which are then used to determine the field coil dimensions, subject to the design rules established by the simplified model.

The voltage for peak power density is governed by the rotor sizing criterion. At low voltage, the current is relatively high and the rotor large. The field coil and excitation requirements are a small component of the total mass.

As the voltage is increased the load current decreases along with the rotor diameter. The field coil mass and excitation requirements increase; however, the total mass is decreasing.

As the voltage is increased beyond optimum, the excitation system becomes the dominate mass and the power density decreases.

The process was repeated for a number of different power levels. Plots of the peak power density, voltage, and efficiency for power levels ranging from 1 to 100 MW are shown in Figure 5. The power density vs. power curve levels off at just above 500 MW.

Fig. 4. Power density vs voltage - 50-MW baseline.

Figure 5 Power Density vs Power Level.

A second set of studies was completed where the current was held fixed (which also implies a fixed rotor radius) and the power allowed to vary as the voltage varied. These studies gave peak power densities for a given voltage and current which were the same as given by the fixed power parametric curves.

CONCLUSIONS

The results of the investigation presented show that there is an optimum power density and voltage for continuous duty "air core" homopolar machines. The studies presented here were for a specific set of machine configurations, thermo-physical constraints, and design rules. Changes in these parameters will result in different optimums.

The two most significant results of this investigation are that: 1) The working or average flux density in the active region, B_a , is not an arbitrarily selectable parameter, but rather is predetermined once the rotor diameter and the operating current densities are selected; and 2) Power densities in excess of 80 kW/kg are obtainable at average flux densities in the active region on the order of 1 T and peak flux densities anywhere within the machine of less than 2.3 T.

It is also significant that the high power levels are achievable at modest current densities and sliding velocities and very low flux density levels.

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APPENDIX B

LOSS CALCULATIONS FOR HIGH SPEED, HIGH CURRENT LEVEL CURRENT COLLECTORS

William F. Hannan III
Westinghouse Electric Corporation
Research and Development Center
1310 Beulah Road
Pittsburgh, PA 15235

ABSTRACT

This paper presents a mathematical model describing the controlling aspects of electrical losses found in high speed current collectors. At high speeds, a "skin effect" is predicted to occur in the rotor conductor due to the periodic spacing of brushes. It is shown that the rotor conductor has a boundary condition which can be expressed as a traveling wave. The three dimensional periodic steady state cylindrical wave functions are solved for geometries approximating those found in homopolar rotor conductors. A strong velocity dependence is demonstrated for the losses calculated from predicted currents. Knowledge and understanding of these losses are important to the designer of high power density, advanced homopolar machines.

INTRODUCTION

The analysis of electrical contacts has been based predominantly on static contacts. The accepted model to calculate contact resistance is found in Holm's work [1]. This model is based upon calculation of the electrical potential between two remotely located contact points. Due to the small areas of actual contact developed and the relatively enormous distances found between such contacts, it is reasonable to calculate the contact resistance attributed to the current "constriction." This model has assumed that the electric fields develop as though the current diffused into the semi-infinite conductor. This model is very useful in gaining insight into the governing phenomena of contact theory. However, the model will not explain certain apparent variations observed in contact resistance, most notably variations due to high velocities.

The model presented by Holm is one dimensional with no time variation. Such a model is incapable of dealing with time variations that are introduced with high velocity current collectors.

In order to explore the basic governing phenomena of a high speed contact, the field equations for a cylindrical conductor have been presented and solved for a sample case similar to a small, high powered homopolar generator (HPG) recently manufactured by Westinghouse. These equations present a three dimensional, periodic steady state analysis that will allow calculation of the electric and magnetic fields occurring in cylindrical conductors. The solutions of these equations are restricted initially to cases that force current flow into the

conductor over the entire apparent area of contact between brush and conductor. Future efforts will be directed towards solutions with the conducting areas collapsed to sizes consistent with the present contact theory.

The model presented is useful for conductors that possess a constant relative permeability, such as copper. Ferromagnetic substances will cause the differential equations that result to become nonlinear, and the closed form solution presented will not be appropriate.

This model is developed to allow an understanding of high velocity current transfer. It suggests that the observed variations in apparent contact resistance due to velocity variations can be explained as consequences of Maxwell's equations.

ANALYTICAL MODEL

In order to understand the loss mechanisms operating in HPG devices, it is necessary to examine the basic electromagnetic fields that develop within the rotor conductor. Toward this end, an analytical model has been constructed. This analytical model is derived from the field equations of a cylindrical field. These equations are solved as a boundary value problem using the radial current injected into the cylindrical surface as the boundary condition to be met.

EQUATIONS OF A CYLINDRICAL FIELD

The following description and derivation of the equations of a cylindrical field are found in "Electromagnetic Theory" by Julius Adams Stratton, First Edition, Ch. 6 [2].

An electromagnetic field cannot, in general, be derived from a purely scalar function of space and time; as a consequence, the analysis of electromagnetic fields is inherently more difficult than the study of heat flow or the transmission of acoustic vibrations. The three-dimensional scalar wave equation is separable in 11 distinct coordinate systems, but complete solutions of the vector wave equation in a form directly applicable to the solution of boundary-value problems are at present known only for certain separable systems of cylindrical coordinates and for spherical coordinates. It will be shown that in such systems an electromagnetic field can be resolved into two partial fields, each derivable from a purely scalar function satisfying the wave equation.

Stratton develops the following formulas for the field vectors in terms of a scalar wave function ψ .

The scalar wave equation is as follows:

$$\Delta^2 \psi - \mu\epsilon \frac{\delta^2 \psi}{\delta t^2} - \mu\sigma \frac{\delta \psi}{\delta t} = 0 \quad (1)$$

In conducting metals, the second term is negligible and will be dropped. Therefore, the scalar wave equation becomes

$$\Delta^2 \psi = \mu\sigma \frac{\delta \psi}{\delta t} = \frac{1}{h^2} \frac{\delta \psi}{\delta t} ; \quad (2)$$

$$h^2 = \frac{1}{\mu\sigma} \quad (3)$$

This equation, transformed into cylindrical coordinates, is:

$$\Delta^2 \psi = \frac{1}{\rho} \frac{\partial}{\partial \rho} \left(\rho \frac{\partial \psi}{\partial \rho} \right) + \frac{1}{\rho} \frac{\partial^2 \psi}{\partial \phi^2} + \frac{\partial^2 \psi}{\partial z^2} = \frac{1}{h^2} \frac{\partial \psi}{\partial t} \quad (4)$$

The coordinate system used in this analysis is as shown in Figure 1.

Figure 1 — Conductor Model Under Analysis

SOLUTION OF SCALAR WAVE EQUATION BY SEPARATION OF VARIABLES

Assume a solution as follows:

$$\psi = R(\rho)P(\phi)Z(z)T(t) \quad (5)$$

Substitution of the assumed solution into the cylindrical wave equation yields:

$$\frac{1}{\rho} \frac{\delta}{\delta \rho} \left[\rho P Z T \frac{\delta R}{\delta \rho} \right] + \frac{1}{\rho^2} R Z T \frac{\delta^2 P}{\delta \phi^2} + R P T \frac{\delta^2 Z}{\delta z^2} = \frac{1}{h^2} R P Z \frac{\delta T}{\delta t} \quad (6)$$

Divide Equation (6) by $R \cdot P \cdot Z \cdot T$ to obtain:

$$\frac{1}{\rho} \frac{\delta}{\delta \rho} \left(\frac{\rho}{R} \frac{\delta R}{\delta \rho} \right) + \frac{1}{\rho^2} \frac{1}{P} \frac{\delta^2 P}{\delta \phi^2} + \frac{1}{Z} \frac{\delta^2 Z}{\delta z^2} = \frac{1}{h^2 T} \frac{\delta T}{\delta t} \quad (7)$$

Each term is now a function of a single variable; therefore the equation can be separated by variables. Partial differentials are shown to be total differentials.

Next, assume that the solutions are of exponential type for P , Z , and T .

$$\text{Assume } \frac{d^2 P}{d\rho^2} = -n^2 P \quad (8)$$

$$\text{Typical solution; } P = A_1 e^{in\phi} + A_2 e^{-in\phi}$$

$$\text{Assume } \frac{d^2 Z}{dz^2} = -m^2 Z \quad (9)$$

$$\text{Typical solution; } Z = B_1 e^{imz} + B_2 e^{-imz}$$

$$\text{Assume } \frac{dT}{dt} = i\omega T \quad (10)$$

Typical solution; $T = C_1 e^{i\omega t}$

Put the assumed solutions back into the original equation to produce:

$$\frac{1}{\rho} \left(\frac{d}{d\rho} \left(\rho \frac{dR}{d\rho} \right) \right) - \frac{1}{\rho^2} n^2 - m^2 + \frac{i\omega}{h^2} = 0 \quad (11)$$

or

$$\frac{1}{\rho} \frac{d}{d\rho} \left(\rho \frac{dR}{d\rho} \right) + \left(\left(\frac{i\omega}{h^2} - m^2 \right) - \frac{n^2}{\rho^2} \right) R = 0 \quad (12)$$

This is a form of Bessel's equation that has the following solutions.

$$R = D_1 J_n \left(\sqrt{\frac{i\omega}{h^2} - m^2} \rho \right) + D_2 H_n \left(\sqrt{\frac{i\omega}{h^2} - m^2} \rho \right) \quad (13)$$

Where J_n is a Bessel function of the first type and H_n is a Bessel function of the second type.

For a first pass, analysis was of a solid cylindrical conductor. In this case

$$H_n \rightarrow \infty \text{ at } \rho = 0; \text{ therefore } D_2 = 0$$

The solution of the scalar wave equation can therefore be shown to be of the form

$$\psi = e^{in\phi} e^{imz} e^{i\omega t} J_n \left(\sqrt{\frac{i\omega}{h^2} - m^2} \rho \right) \quad (14)$$

The field equations for such a geometry and potential are given in "Electromagnetic Theory," by Julius Stratton, 1st Ed. pp. 360-361 [2].

$$E_r = ih \sum_n a_n \frac{\delta \psi_n}{\delta r} - \frac{\mu \omega}{r} \sum_n b_n \psi_n \quad (15)$$

$$E_\phi = -\frac{h}{r} \sum_n n a_n \psi_n - i\mu \omega \sum_n b_n \frac{\delta \psi_n}{\delta r} \quad (16)$$

$$E_z = (k^2 - h^2) \sum_n a_n \psi_n \quad (17)$$

where the following substitutions have occurred.

$$k^2 = \frac{i\omega}{h^2} \quad (18)$$

$$h^2 = m^2 \quad (19)$$

From initial conditions prescribed over a given plane or cylindrical surface, a solution is constructed by superposition.

The coefficients a_n and b_n are determined from boundary conditions imposed upon the cylinder.

MATCHING BOUNDARY CONDITIONS

The boundary conditions imposed upon the cylinder are

- 1) Axial current density is zero at $z = 0, 2c$.
- 2) Radial current density is uniformly injected into cylinder at $r = a$ only in areas defined as those where brush contact is established.

$$J_z|_{z=0,2c} = 0 = \sigma E_z = \sigma \left(\frac{i\omega}{h^2} - m^2 \right) a_n \phi_n \quad (20)$$

For non-trivial solution,

$$\phi_n|_{z=0,2c} = 0 = R(\rho)P(\phi)Z(z)T(t) \quad (21)$$

Since $R(\rho)$ $P(\phi)$ and $T(t)$ are generally not zero

$$Z(z)|_{z=0,2c} = 0 = B_1 e^{imz} + B_2 e^{-imz} \quad (22)$$

An equivalent statement using sin and cos in place of exponential form is

$$Z(z)|_{z=0,2c} = 0 = B_1' \sin mz + B_2' \cos mz \quad (23)$$

$$B_2' = 0 \quad (24)$$

$$0 = B_1' \sin 2mc \quad (25)$$

This is true if $2mc = a\pi$

$$m_a = \frac{a\pi}{2c} \quad (26)$$

The radial current density at $\rho = a$ can be expressed mathematically as;

$$J_r|_{r=a} = \sum_n \sum_a J_{n,a} (\cos(n(\phi + \omega t))) \cos m_a z \quad (27)$$

where $J_{n,a}$ are the Fourier coefficients of a traveling wave moving with velocity ω .

The values of $J_{n,a}$ are found by considering the Fourier series to be a product of a series in the ϕ direction and a series in the Z direction.

$$J_{n,a} = F_n \cdot G_a \quad (28)$$

Where F_n is the n^{th} Fourier coefficient in the ϕ series and G_a is the a^{th} Fourier coefficient in the Z series.

This can best be shown by an example. In the sample case it is assumed that the current is "on" for one-half of a brush pitch and "off" for the remainder. Consider one brush pitch equal to 2π .

Inspection of Figure 2 shows that the match between the ideal waveform and the Fourier approximation with eleven terms is in good agreement. Little error is introduced by not considering higher harmonic terms in the circumferential Fourier approximation.

The same strategy is employed to derive the Fourier coefficients in the axial direction. A figure showing the ideal waveform, and the Fourier approximation which results from using fifteen terms is displayed as Figure 3.

Inspection of the figure shows that consideration of only fifteen Fourier terms will tend to ignore space harmonics of short

Figure 2 — Circumferential Fourier Series

Figure 3 — Axial Fourier Series

wavelength. It is these high order harmonics which will tend to "square off" the shoulders of the Fourier approximation used in calculation of the field equations.

The basic restraint against using high numbers of Fourier coefficients is that the ϕ and the z coefficients are combined as products. Thus, an analysis using 10 terms in the ϕ direction and 10 terms in the Z direction actually calculates 100 separate terms in each summation. Doubling the number of terms utilized will quadruple the calculation effort.

The field equations become:

$$\frac{J_r}{\sigma} = E_r = ih \sum_a \sum_n a_{n,a} \frac{\delta \psi_{n,a}}{\delta r} - \frac{\mu \omega}{r} \sum_a \sum_n n b_{n,a} \psi_{n,a} \quad (29)$$

$$\frac{J_\phi}{\phi} = E_\phi = -\frac{h}{r} \sum_a \sum_n n a_{n,a} \psi_{n,a} - i\mu \omega \sum_a \sum_n b_{n,a} \frac{\delta \psi_{n,a}}{\delta r} \quad (30)$$

$$\frac{J_z}{\sigma} = E_z = (k^2 - h^2) \sum_a \sum_n a_{n,a} \psi_{n,a} \quad (31)$$

These equations contain the two partial fields to which Stratton refers. One field is found when considering only the terms containing a_n as a factor, while the other is found to contain only terms having b_n as a factor.

The first field referred to above is a field of electric type (transverse magnetic field) while the second is a field of magnetic type (transverse electric field).

In order to calculate the degree to which each type field is excited for each mode investigated, it is necessary to review how the current injected at the surface excites each mode.

For the sample case, consider excitation of the $a = 1, n = 1$ mode. Since all the modes except the DC mode ($n = 0$) are forced at frequencies at or above brush passing frequency, a very strong surface effect is experienced. All these higher modes exhibit a strong "skin effect."

Examination of the equations shows that the electric type field establishes very large current flow in the Z direction with virtually no flow in the ϕ direction, while the magnetic type field establishes current flow in the ϕ direction with absolutely no flow in the Z direction.

This observation will allow an estimation to be made as to how strongly each field will be excited.

Figure 4 represents a segment of the rotor surface that extends to a depth equal to the skin depth "d". The dimensions chosen for the ϕ and Z directions correspond to the Fourier wavelengths of the mode under investigation. Since the rotor radius is usually large in comparison to $\lambda_{\phi,a}$, it is reasonable to show this figure in Cartesian rather than cylindrical coordinates.

Since various modes are excited as both a and n vary, the following notation for subscripting will be utilized.

$R_{\phi,a,n}$ = Surface resistance in ϕ direction for the $(a,n)^{th}$ mode

$\lambda_{\phi,a}$ = Fourier wavelength in ϕ direction for a^{th} term.

$\lambda_{z,n}$ = Fourier wavelength in z direction for n^{th} term

$d_{a,n}$ = "Skin depth" for $(a,n)^{\text{th}}$ mode

$I_{z,a,n}$ = Surface current in Z direction for the $(a,n)^{\text{th}}$ mode

δ = Electrical conductivity of material

Since Figure 4 represents excitation by sine waves of equal amplitude (but differing wavelength) in the ϕ and Z direction, an estimate of the division of current flow in each direction is made as follows:

$$R = \frac{\lambda}{d\sigma} \quad (32)$$

from "Electrodynamics" by Arnold Sommerfeld, pg. 165 [3].

Figure 4 — Representation of excitation of $(i,j)^{\text{TH}}$ mode, assuming skin phenomena can be modeled as restricted to skin depth "d".

$$R_{\phi,a,n} = \frac{\lambda_{\phi,a}}{d_{a,n}\sigma} \quad \text{and} \quad R_{y,a,n} = \frac{\lambda_{z,n}}{d_{a,n}\sigma} \quad (33)$$

If the model is considered to represent a source (located at (1)) and a sink (located at (2)) which causes current to flow in the ϕ direction, and also a source (located at (1)) and a sink (located at (3)) causing current to flow in the Z direction, then the following is found to be true.

$\Delta V_{12} = \Delta V_{13}$, since both are excited by sine waves of equal amplitude.

$$\Delta V_{12} = I_{\phi,a,n} R_{\phi,a,n} = I_{\phi,a,n} \frac{\lambda_{\phi,a}}{d_{a,n}\sigma} \quad (34)$$

$$\Delta V_{13} = I_{z,a,n} R_{z,a,n} = I_{z,a,n} \frac{\lambda_{z,n}}{d_{a,n}\sigma} \quad (35)$$

$$I_{\phi,a,n} \frac{\lambda_{\phi,a}}{d_{a,n}\sigma} = I_{z,a,n} \frac{\lambda_{z,n}}{d_{a,n}\sigma} \quad (36)$$

$$\frac{I_{\phi,a,n}}{I_{z,a,n}} = \frac{\lambda_{z,n}}{\lambda_{\phi,a}} \quad (37)$$

This suggests that the ratios of wave length of each separate mode are in inverse proportion to the current flow excited between the two fields considered.

In the sample case for a brush spacing

$$\lambda_{\phi,1} \sim \frac{\pi \cdot D}{\frac{\pi}{2} \text{ brushes in one brush row}} = \frac{\pi \cdot 5 \text{ in.}}{16} = 0.982 \text{ in.} \quad (38)$$

$$\lambda_{z,1} = \text{Rotor length} \approx 5.2 \text{ in.} \quad (39)$$

$$\text{Ratio of } \frac{\text{Axial Current}}{\text{Radial Current}} = \frac{0.982}{5.2} = 0.19 \text{ for } \alpha = 1, n = 1 \text{ mode} \quad (40)$$

This shows that an amount of ac current is excited to flow in the axial direction. This current causes significant parasitic losses.

The field equations were programmed using the above estimations, and the results will be discussed in the next section.

SAMPLE ANALYSIS

The cylindrical field equations were solved for the following geometry and material properties:

Rotor radius = 6.35 cm (2.5 in.) = a

Rotor length = 13.21 (5.2 in.) = 2c

Axial distance between brushes = 9.14 cm (3.6 in.) = 2b

Axial length of brushes = 0.63 cm (0.25 in.)

Circumferential $\frac{\pi}{2}$ brushes = 16

Length of brush pitch = $2\pi a/16 = 2.5 \text{ cm (0.982 in.)}$

Brush width = 1.25 cm (0.491 in.)

Rubbing velocities = 50 m/s, 100 m/s, 200 m/s

Brush passing frequency at 200 m/s = 8 kHz

Conductivity = 100% IACS

Number of terms in axial direction = 15

Number of terms in circumferential direction = 11

Boundary conditions were matched using Fourier series of 11 terms to describe the circumferential variation and 15 terms to describe the axial variation. Figures 2, 3, 5 and 6 demonstrate the actual boundary conditions imposed for this analysis. The number of terms used was limited by computation time and cost. A more accurate estimate will result if the number of Fourier terms is increased. However, the analysis performed appears to adequately represent the problem at hand.

Figures 5 and 6 show the assumed boundary conditions imposed at the rotor surface upon the radial current density. Figures 7 through 13 present the results of this analysis for all three dimensions for a peripheral rubbing velocity of 100 m/s. Review of the output shows that a "skin effect" is predicted to occur in all components of the current densities. Rapid variations in both the radial and circumferential current densities is predicted to occur within this "skin" in the immediate area of the current collectors. Special note should be taken of the dc components present in the radial and axial current densities.

Figures 8 and 9 depict axial current density. These figures show that a large surface current consisting predominantly of ac current with a small dc offset is established and persists down the rotor between the current collection areas. It is believed with an increase in the number of Fourier terms considered in this analysis that the

largest peaks located at $\phi = \pi$ and 2π will become slightly larger in magnitude, while the smaller peaks will be reduced.

The variation of this phenomenon with rubbing velocities can be seen by considering Figures 14 through 19, which show the radial variations in current densities occurring at velocities of 50 and 200 m/s. Comparison with the previous figures (100 m/s) shows that the magnitude of the surface density and the skin depth predicted are both highly dependent on the rubbing velocity.

A significant parasitic loss can be driven by these high frequency surface currents. The prediction of the magnitude of these losses requires further analysis, exploring the ramifications of forcing the current transfer to occur across surfaces that will approach the size of "a" spots.

SUMMARY

An electromagnetic field is established in a homopolar rotor conductor due to the distributed nature and relative peripheral velocity of radially-directed current injected into the rotor surface. This electromagnetic field is determined for the case of a solid cylindrical conductor. The predicted fields consist of a dc field superimposed with a time varying (ac) field. The ac field is shown to cause high current densities to occur which are confined to a narrow "skin" at the conductor surface. The estimated magnitudes of the predicted currents show that at high currents and at high velocities, excess loss will occur due to these ac currents.

An analytical model has been presented to explain such observations as the velocity dependence of contact resistance. This model will allow the estimation of losses occurring in high powered homopolar machines to be performed.

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Figure 5 — Variation of Radial Current Density at Rotor Surface Versus Circumferential Location for $Z = 0.725$ (Midpoint of Brushtrack). This represents the boundary condition imposed upon the rotor conductor for all peripheral speeds (50, 100, 200 m/s) investigated. Note that the DC offset represents the average radial current entering the cylinder and that a good approximation to a square wave has been attained using 11 Fourier terms for the circumferential representation.

Figure 6 — Variation of Radial Current Density at Rotor Surface Versus Axial Location for $\phi = \pi/2; \pi; 3\pi/2$. This represents the boundary condition imposed upon the rotor conductor for all peripheral speeds (50, 100, 200) investigated. Note the match to assumed "square" input can be improved through use of more Fourier terms.

Figure 7 — Variation of Radial Current Density Versus Radius for a Rubbing Velocity of 100 m/s and $\phi = \pi/2$; π ; $3\pi/2$. The value of the radial current density at $\phi = \pi$ is identical to the DC value which results if current were injected into the rotor through a surface band defined by the brushtrack. The skin depth is apparent in this case and can be compared to similar Figures 14 and 17 which depict different rubbing velocities.

Figure 8 — Variation of Axial Current Density at Rotor Surface Versus Circumferential Location for $Z = .725$ (Midpoint of Brushtrack) and Rubbing Velocity = 100 m/s. Note the rather pronounced peaks evident about 0, π and 2π . These peaks are established about the points of current interruption (current is either made or broken at multiples of π).

Figure 9 — Variation of Axial Current Density at Rotor Surface Versus Axial Location for $\phi = 2.932, \pi, 3.351$ and Rubbing Velocity = 100 m/s. This figure shows the axial extent of the AC surface phenomena. The peaks extend virtually unchanged for the length between the current collectors.

Figure 10 — Variation of Axial Current Density with Radius for $\phi = 2.93; \pi; 3.35, Z = 0.725$, and Rubbing Velocity = 100 m/s. The skin depth is apparent in this case and can be compared to similar Figures 15 and 18 which depict different rubbing velocities.

Figure 11 — Variation of Circumferential Current Density at Rotor Surface Versus Axial Location for $\phi = 0; \pi/2; \pi$ and Rubbing Velocity = 100 m/s.

Figure 12 — Variation of Circumferential Current Density at Rotor Surface Versus Circumferential Location for $Z = 0.725$ (Midpoint of Brushtrack) and Rubbing Velocity = 100 m/s. This variation is very pronounced only in the immediate vicinity of the brush track.

Figure 13 — Variation of Circumferential Current Density with Radius for $\phi = 0; \pi/2; \pi$ and Rubbing Velocity = 100 m/s. The skin depth is apparent in this case, and should be compared to similar Figures 16 and 19, which depict different rubbing velocities.

Figure 14 — Variation of Radial Current Density Versus Radius for a Rubbing Velocity of 50 m/s and for $\phi = \pi/2; \pi; 3\pi/2$ at $Z = 0.725$

Figure 15 — Variation of Axial Current Density Versus Radius for a Rubbing Velocity of 50 m/s and for $\phi = 2.93, \pi, 3.351$ at $Z = 0.725$

Figure 16 — Variation of Circumferential Current Density Versus Radius for a Rubbing Velocity of 50 m/s for $\phi = 0; \pi/2; \pi$ at $Z = 0.725$

Figure 17 — Variation of Radial Current Density Versus Radius for a Rubbing Velocity of 200 m/s and for $\phi = \pi/2; \pi; 3\pi/2$ at $Z = 0.725$

Figure 18 — Variation of Axial Current Density Versus Radius for a Rubbing Velocity of 200 m/s and for $\phi = 2.93, \pi, 3.35$ at $Z = 0.725$

Figure 19 — Variation of Circumferential Current Density Versus Radius for a Rubbing Velocity of 200 m/s and for $\phi = 0; \pi/2; \pi$ at $Z = 0.725$

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APPENDIX C
HOMOPOLAR MACHINE APPLICATION STUDY

Dennis Pavlik
Westinghouse R&D Center

B.1 BACKGROUND

The objective of the Machine Application Study was to define a homopolar power supply which was suitable for the SDI mission requiring hundreds of megawatts of continuous dc power in the 50- to 400-Volt range. The major operational requirement of the system being that the power density exceed 80 kW/kg.

To define the operational space and functional design requirements for the current collection system, a baseline homopolar machine concept (which met the operational goals of the SDI mission) was selected. In particular, the selection of a baseline machine quantified the cooling fluids, rubbing speed, current densities, and the thermal and electromagnetic environments for the current collection system.

Two basic homopolar machine concepts were evaluated as part of the selection of a baseline machine. These were the "Iron Core" and "Air Core" machine arrangements. All concepts employed drum type construction (refer to Figure B-1), as previous studies^{1,2} have shown these to have the highest power density and voltage generating capability.

"Iron Core" refers to those machines which are constructed primarily of a ferromagnetic material (i.e., iron or steel). In addition to its structural function the "iron" provides a low reluctance path for the magnetic flux created by the field winding and greatly reduces the excitation requirements on the field winding. Because of the magnetic saturation of ferromagnetic materials, operation of "Iron Core" homopolar machines at flux densities much above 2 tesla is not practical.

"Air Core" refers to those machines which are constructed primarily of nonferromagnetic materials such as stainless steel,

Figure B-1 — Ideal Drum Type Homopolar Motor.

aluminum, copper, fiber composites, etc. "Air" refers to the fact that the materials used in their construction have roughly the same magnetic permeability as vacuo or air. In these machines there is no low reluctance path for the flux to follow. The flux pattern within the machine is controlled by the shape and placement of the field windings. Because the excitation requirements will in general be very high the field coils are almost always cryogenically cooled.

Previous studies¹ for high voltage pulsed power systems have shown that "Air Core" machines will have higher power densities than "Iron Core" designs of the same rating. This is because the structural members in "Air Core" machines need only be designed for mechanical strength and not to carry magnetic flux. The savings in the weight of the structural materials more than offsets the added weight of the larger field coil. However, these studies dealt with very high voltage (thousands of volts) homopolar machines which required magnetic field strengths on the order of 4 to 8 tesla. At these high magnetic field strengths there is no advantage to using ferromagnetic materials.

Because low voltage (50 to several hundred volts), high power density, high efficiency machines have not been previously investigated a benchmark assessment of "Iron Core" technology was included as part of the application studies. The results of these studies will now be presented.

B.2 IRON CORE DESIGN STUDIES

To establish the limits on "Iron Core" technology for continuous duty applications, a series of parametric studies was undertaken. In these studies, the air gap flux density was fixed at 1.7 tesla. Increasing the flux density to 2 tesla will result in higher power densities. However, the practical operational limit for ferromagnetic materials is near the 1.7 tesla value. For these studies the rotor tip speed was fixed at 200 meters/sec (7874 ips) and the packing factor for the brushes was fixed at 0.3. Two cooling schemes were evaluated: Scheme 1 - Cryogenically cooled field coils with a water cooled current collection system and rotor. Scheme 2 - Water cooled field coils,

rotor, and current collection system. The basic difference between these techniques is their effect on the operating current densities for the different components. Table B-1 identifies the schemes and the differing operating points.

TABLE B-1
DIFFERING OPERATING PARAMETERS FOR THE IRON CORE SCHEMES

SCHEME	1	1 A	2
ROTOR TIP SPEED - VEL (m/s)	200	200	200
BRUSH CURRENT DENSITY - JBR (MA/sq.m)	20.0	20.0	11.6
ROTOR CURRENT DENSITY - JRC (MA/sq.m)	20.0	38.75	20.0
FIELD COIL CURNT. DEN. - JFC (MA/sq.m)	77.5	77.5	20.0

Another scheme that was briefly considered was an all cryogenically cooled "Iron Core" system. However, the mechanical properties of magnetic steels are significantly degraded at cryogenic temperatures. The problems associated with interfacing a cryogenically cooled rotor conductor with a magnetic steel rotor make that scheme impractical and therefore it was not considered.

Figures B-2, B-3, and B-4 are graphs of the power density, efficiency, rotor speed, and terminal voltage versus machine power for schemes 1, 1A, and 2 respectively. As expected, the cryogenically cooled field coil results in an increased power density and efficiency as compared to the all water cooled case.

The maximum power density obtainable at 100 MW is 29 kW/kg for the cryogenically cooled field coil (scheme 1A). This scheme operates at a 72% higher brush current density than scheme 1.

Although efficiency is increasing with increasing power it seems that the 97% value is a barrier for the "Iron Core" technology when

Figure B-2 — Iron Core Machine Power Density Curve.

Curve 756152-A

Figure B-3 — Iron Core Machine Power Density Curve.

Figure B-4 — Iron Core Machine Power Density Curve.

operating at maximum power density given the present limits on brush current density, contact resistance, friction coefficient, and brush pressure. This is because losses are dominated by the brush friction and contact losses when operating at maximum power density.

Because the brush friction and contact loss are proportional to the load current, raising the operating voltage results in increased efficiency. However, for a fixed rotor velocity this also implies an increase in machine size and the field coil losses become significant and the efficiency begins to drop.

The work completed to date suggests that a continuous duty "Iron Core" based homopolar machine will not meet the 80 kW/kg power density requirements for the SDI low voltage (50 to 100 volts) multimegawatt mission.

Although inadequate for the SDI mission, the power densities which are obtainable using "Iron Core" technology are quite impressive when compared to conventional rotating electrical machinery. The benchmark studies have shown that at power levels below 2 MW the "Iron Core" machines are competitive in terms of efficiency and power density with "Air Core" designs!

Increasing the rotor tip speed and brush current density does not address the question of low efficiency. Unless the efficiency can be increased, the cooling fluid requirements for a 100 MW machine become too large. To meet the efficiency requirements the present friction coefficient and contact resistance limits must be reduced significantly.

Given the present limits on brush current density and rotor tip speed, the maximum obtainable power density approaches 30 kW/kg at an efficiency of 96% which is significantly below the SDI mission requirements. Further increases in power density for this technology can only be achieved by dramatically increasing the rotor tip speed and/or brush current density.

B.3 AIR CORE DESIGN STUDIES

B.3.1 Development of a Design Algorithm and Optimization Criteria

In general, "Air Core" homopolar machines have been designed on a case by case basis to meet a specific set of performance requirements. To make a systematic evaluation of the general performance characteristics and to identify the operational parameter space for "Air Core" designs it was necessary to develop an optimization criteria and design algorithm for these machines.

The two key differences between the "Air Core" and "Iron Core" designs are:

- 1) In an "Iron Core" machine the magnetic circuit and operating flux density defines the machine performance and all major dimensions such as the rotor diameter and weights. In "Air Core" machines the rotor diameter is the key independent physical parameter and it determines the magnetic circuit and operating flux density in addition to establishing the basic machine dimensions, weight, rpm (for a fixed tip speed), and power density.
- 2) In the "Air Core" algorithm the magnetic circuit equations are formulated in terms of the magnetic vector potential. The operational magnetic vector potential is simply the required internal voltage divided by the rotor tip speed (volt-sec/meter = webers/meter).

By formulating the magnetic field and voltage equations for the homopolar machine in terms of the magnetic vector potential, a unique optimum power density machine can be configured for a specified rotor diameter. This assumes that the limits on rotor tip speed, current densities in the brushes, rotor conductor, and field coils are specified along with a shape factor on the field coil as is done with "Iron Core" design algorithms.

Using the magnetic vector potential formulation and an idealized machine configuration based on infinitesimal current sources for the field winding, it was shown that the machine power density (kW/kg) depends on the difference in magnetic vector potential between the sets

of brushes, divided by their spacing. This is equal to the average radial flux density in the active region, which means that the power density will be maximized when the average flux density in the active region is maximized. Assuming, of course, that the rotor tip speed, rotor radius and load current are fixed.

Figure B-5 shows a plot of the magnetic vector potential (for a single coil) and the average flux density versus brush spacing at a fixed radius. B_{avg} and power density (PDEN) peak when the brushes are spaced so that the internal voltage of the machine (E) is 50% of the maximum possible value that can be obtained.

The model was expanded to include finite dimensions and weights for the field coil. Subsequent studies which included the field coil weight and dimensions indicated that a machine designed using the 50% voltage criteria still produced the highest power density machine.

The rotor size is set by how much heat is generated (brush friction, rotor resistance, I^2R) and the area needed to manage the coolant fluids. The coil size is set by the field coil current density limit (fill factor and coolant management). The brush axial spacing, L , is set by the 50% voltage criteria for maximum power density; it is a function of the ratio of the rotor radius to the radius at the magnetic centroid of the field coil.

The simplified model and analysis confirmed that a complete "Air Core" machine can be formulated using the same constraints as the "Iron Core" algorithm (i.e., rotor tip speed, current densities, packing factors, etc.), and two added constraints; a field coil shape and aspect ratio constraint, and a fixed rotor diameter. Furthermore, if the 50% voltage criteria is used this machine will operate at the maximum power density.

The magnetic vector potential approach was verified using an idealized homopolar machine model. It was then incorporated into a more comprehensive design code which considered all major machine components. Several machines were configured using the design optimization algorithm. The code was then verified by comparing its results to a

Curve 756148-A

Figure B-5 — Variation in Vector Potential and Average Flux Density as a Function of Brush Spacing

finite element model of the magnetic circuit and hand calculations for the structural and thermal performance.

B.3.2 Parametric Studies

The objectives of the parametric studies were two fold. The first was to verify the optimization criteria and investigate different machine configurations. The second was to establish the performance criteria and design requirements for the current collection system.

One element of the initial parametric studies was an attempt to maximize the average flux density in the active region (B_{avg}), as this is directly proportional to the machine power density. However, as previously mentioned, the studies showed that B_{avg} is not arbitrarily selectable if the machine is to be operated at maximum power density.

B_{avg} is not an arbitrarily selectable quantity because the useful flux through the rotor depends on the ratio of the rotor radius to that of the field coil centroid and the ampere turns in the coil. Any increase in the field coil's excitation level must result in an increase in coil cross sectional area due to thermal constraints. This can be accomplished by changing the coil radial thickness, axial length, or a combination of the two.

Changing the radial coil thickness will result in a lower flux density, and hence, lower power density machine. This is because the machine flux increases linearly with excitation level but elliptically with changes in the ratio of rotor to coil radius. This means that the decrease in useful flux due to changing R_{rot}/R_{coil} is much greater than the increase due to greater excitation.

Increasing the axial length of the coil to get extra ampere turns works to some extent, but eventually the coil length results in a brush spacing greater than the 50% voltage criteria would establish and further increases actually decrease B_{avg} and power density.

A rectangular coil cross section (with the long dimension along the axis) was selected for all field coil designs for the following reasons:

- 1) A rectangular shape maximizes the ratio of R_{rot}/R_c which in turn increases the useful flux per ampere (this is not the same as maximizing the inductance of the coil).
- 2) It has the minimum volume for a fixed ampere turn excitation, which in turn gives the lowest loss and lightest weight.
- 3) The field coil must also be used to shape the flux profile in the current collection region to prevent excessive radial flux and hence circulating currents. To accomplish this requires a coil of a generally rectangular cross section which is at least twice as long as the brush box.

For the range of parameters investigated a coil which is twice as long as the radial thickness (aspect ratio of 2) seems to be a good compromise, although some further investigation is appropriate.

The selection of the hollow rotor design concept and the field coil geometrical constraints completed the set of design rules for "Air Core" continuous duty machines. The next phase of the parametric studies were used to define the current collector design specifications and parameter space. Table B-2 gives the design parameters used in the those studies.

Figure B-6 shows a typical power density (PDEN) versus voltage curve for a 50 MW homopolar using the design constraints of Table B-2. Observe, that just as with iron core designs, there is a voltage where PDEN peaks. For the 50 MW machine PDEN peaks at 80 kW/kg between 50 and 53 volts. The efficiency is 96.2%, but can be improved upon by changes in the current collector as will be discussed in more detail in the next section.

50 MW BASELINE AIR CORE MACHINE

Figure B-6 — Power Density Vs. Voltage for a 50 MW Homopolar.

Figure B-7 — Air Core Machine Power Density Curve.

Curve 756149-A

Figure B-8 — Air Core Machine Power Density Curve.

TABLE B-2
AIR CORE DESIGN CONSTRAINTS

PARAMETER	VALUE	UNITS
FIELD COIL COOLING	LH2	HYPERCONDUCTING
ROTOR COOLING	LH2	HYPERCONDUCTING
ROTOR TIP SPEED	200.	meters/sec.
BRUSH CURRENT DENSITY	20.0 (0.3)	kA/cm ² (packing fraction)
FIELD CURRENT DENSITY	77.5 (0.4)	kA/cm ² (packing fraction)
ROTOR CURRENT DENSITY	77.5 (1.0)	kA/cm ² (packing fraction)
ROTOR RESISTIVITY	0.021	μ -Ohm-cm
FIELD COIL RESISTIVITY	0.021	μ -Ohm-cm
BRUSH RESISTANCE (cm ²)	0.001	Ohms/brush
FRICTION COEFICIENT	0.10	-----
ROTOR COOLING FACTOR	1.484	kW/cm ²

A set of power density vs voltage curves was made for output powers ranging from 0.5 MW to 10,000 MW. These curves were compiled into a composite curve showing peak power density vs power as shown in Figure B-7. A more detailed set of data showing not only power density but also machine voltage and efficiency vs power for the 0.5 Mw to 100 MW power range is shown in Figure B-8.

In Figure B-8 the efficiency curve peaks at about 97%. This can be improved by changing the operating current density of the brushes. In a current collector the minimum loss condition occurs when the friction loss and I^2R contact loss are equal. In the systems considered, the friction losses are only 10% of the total losses and the I^2R contact losses are 81% of the total. Therefore, machine efficiency can be increased by lowering the brush current density and increasing the number of brushes.

An investigation of the impact of varying the current collector defining parameters is planned for 1988.

B.3.3 Baseline Machine Selection

In the final segment of the application study a 50 MW "Air Core" homopolar machine was configured to serve as a baseline. The 50 MW rating was selected because it was large enough to be useful in the SDI mission scenario and small enough so that it could be built and tested.

As shown in Figure B-5, the machine had a power density of 80 kW/kg using the current collector design goals and the hollow rotor concept. The efficiency was 96.2%, but this can be increased by relaxing the brush current density goal of 2 kA/cm^2 . The basic physical dimensions are shown in Figure B-8. Figure B-9 shows a breakdown of the weight and loss for the major machine components.

The most surprising finding was that the average magnetic field, B_{avg} , was only 1.04 tesla and that the peak field in the system was below 2.3 T. This is clearly within demonstrated limits of operating current collectors (iron core experience).

B.4 CONCLUSIONS AND RECOMMENDATIONS

The work completed to date suggests that rather impressive power densities are obtainable using "Iron Core" technology. However, given the present limits on brush current density and rotor tip speed, the maximum obtainable power density approaches 30 kW/kg at an efficiency of 96%, which is short of the SDI requirements. Further increases in energy density for this technology can only be achieved by dramatically increasing the rotor tip speed and/or brush current density. It does not appear that sufficient breakthroughs in this technology can be made to make it viable for the low voltage (50 to 100 volts) SDI application.

"Iron Core" homopolar machines are not suited for the SDI mission requiring multimegawatt, low dc voltage, continuous operation, for the following reasons:

- 1) To meet the SDI mission requirements the machine will require cryogenic cooling. The physical properties of magnetic steels are significantly degraded at these temperatures, and interfaces between the magnetic steel

rotor and stator with the cryogenic components would require significant developments in the state of the art.

- 2) To achieve the power density increases necessary to meet SDI goals, the brush current density and rotor tip speed limits will have to be significantly increased. Since any developments in this area apply equally well to "Air Core" technology, which already has significantly higher power densities, this work should be directed toward those machines as the benefits will be greater.
- 3) The minimum efficiency requirement keeps the cooling requirement in line with fuel consumption. To achieve this minimum efficiency means that the friction coefficient and contact resistance will have to be significantly lowered. It also means that the homopolar machine will have to operate at less than the maximum power density point. The higher tip speed is also working against the efficiency requirement. Again, since an "Air Core" technology-based machine has an inherent advantage in terms of power density, it can more readily be operated at "off peak" conditions and appears to be more attractive.

Previous work has indicated that "Air Core" homopolar machines are viable for the medium voltage (several thousand volts), multimegawatt pulsed duty SDI application. The application study conducted for this program shows that they are also applicable for the low voltage continuous duty application as well. The major problem in the low voltage application will be to maintain a high power density at high efficiency. The critical factors are rotor tip speed, contact resistance, brush friction, and operating current density.

The most significant findings of the study are:

- 1) The average magnetic field in the active region of "Air Core" homopolar machines (operating at maximum power density) is not an arbitrary quantity, but depends on the rotor diameter and the thermal properties and cooling technique employed in the design.

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