DOE/PC/91155--T2

**Coal-Fired High Performance
Power Generating System**

MAY 25 1993

DE-AC22-92PC91155

Quarterly Progress Report

for the Period October 1 - December 31, 1992

Prepared for

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Executive Summary

This report covers work carried out under Task 2, Concept Definition and Analysis, and Task 3, Preliminary R and D, under contract DE-AC22-92PC91155, "Engineering Development of a Coal Fired High Performance Power Generation System" between DOE Pittsburgh Energy Technology Center and United Technologies Research Center.

The goals of the program are to develop a coal-fired high performance power generation system (HIPPS) by the year 2000 that is capable of

- > 47% thermal efficiency
- NO_x , SO_x and Particulates $\leq 25\%$ NSPS
- cost $\geq 65\%$ of heat input
- all solid wastes benign

In order to achieve these goals our team has outlined a research plan based on an optimized analysis of a 250 MW_e combined cycle system applicable to both frame type and aeroderivative gas turbines. Under the constraints of the cycle analysis we have designed a high temperature advanced furnace (HITAF) which integrates several combustor and air heater designs with appropriate ash management procedures.

The Cycle Optimization effort under Task 2 outlines the evolution of our designs. The basic combined cycle approach now includes exhaust gas recirculation to quench the flue gas before it enters the convective air heater. By selecting the quench gas from a downstream location it will be clean enough and cool enough (ca. 300F) to be driven by a commercially available fan and still minimize the volume of the convective air heater.

Further modeling studies on the long axial flame, under Task 3, have demonstrated that this configuration is capable of providing the necessary energy flux to the radiant air panels. This flame with its controlled mixing constrains the combustion to take place in a fuel rich environment, thus minimizing the NO_x production. Recent calculations indicate that the NO_x produced is low enough that the SNCR section can further reduce it to within the DOE goal of 0.15 lbs/MBTU of fuel input.

Also under Task 3 the air heater design optimization continued. For the radiant panels, a rectangular cross section was found to be optimal for minimizing the pressure drop and improving heat transfer. In addition the maximum surface temperature of the convective air heater will be held below 1700F to avoid rapid sintering of the ash. Also reported under this task are the initial corrosion tests on the refractory coating for the radiant panels. Initial results showed rapid corrosion of the refractories containing phosphate binders. This result was not unexpected and really served to confirm predictions. Future studies will focus on silicon carbide and alumina silicate refractories without phosphate binders.

Several milestones have been achieved during this reporting period. The NO_x emissions constraints have been determined and are now understood even in the more complicated HITAF configuration employing radiant panels. All of the design goals for the air heaters, both convective and radiative, have been achieved and the performance evaluation is complete. Finally, all of the inorganics in the four selected coals have been determined.

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TASK 2.1 CYCLE OPTIMIZATION

As more details are available on the requirements of the HITAF coal combustor and heat exchangers, the baseline cycle described in the previous report has been revised. This is an iteration process in which the effects of HITAF variations are evaluated and the performance trends used to guide the next round of HITAF perturbations. In addition, effort continues on adapting the Humid Air Turbine (HAT) cycle based on the Pratt & Whitney advanced aero-derivative FT4000 engine.

Baseline Cycle Revisions

The revised system configuration (Fig. 1) maximizes the temperature in the radiant section by reducing excess air, while maintaining the coal feed rate that had been established previously. Resultant cycle performance is relatively unchanged with an efficiency of 50% based on LHV and a methane fraction of 30%. The high coal feed rate was found to be necessary when radiation effects were integrated into the REI HITAF combustor model. As a result, the calculated temperature of the combustion gas leaving the radiant section is at or above 3,000F as opposed to the 2,400F value that had been assumed previously. A schematic showing major heat and mass balances around the HITAF is presented in Fig. 2. These configurations are based on the Utah Blind Canyon coal.

While the current combustor calculations are based on stoichiometric combustion, in doing the performance calculation an overall amount of 5% excess air has been assumed. This is intended to account for the addition of some burnout air prior to quenching the gas to a temperature level suitable for use in the convective air heater. A water wall section downstream of the radiant section is assumed to drop the gas temperature by 150F prior to mixing with the recirculated flue gas. Mixing reduces the temperature to 1,970F. A slag screen further reduces the gas temperature by 150F prior to entering the convective air heater. The 1,820F air heater inlet temperature was chosen to reduce the potential for buildup of nonremovable deposits on the heat exchange surfaces. Both the water wall and slag screen surfaces are used to raise steam at 682F.

Following the convective air heater, steam superheating and reheating are done to reduce the gas temperature to 1,000F prior to recirculation. The remaining combustion gas is used in a boiler and economizer where its temperature is dropped to 300F prior to sulfur removal.

In the configuration shown, only that part of the gas turbine exhaust needed for combustion is used in the HITAF heat recovery path. The remainder goes directly to the stack after passing through the heat recovery steam generator. This configuration is representative of the three being considered. Cycle efficiency should be affected only in a small way by selection of one over the others. The first, or baseline (Fig. 1), shown here uses gas turbine exhaust in the combustor and flue gas recirculation to quench the hot gases for use in the convective heater. In the second arrangement (Fig. 3), gas turbine exhaust would be used to quench the hot gas. This results in a cleaner schematic but requires that all of the turbine exhaust be treated in the FGD system.

Thermodynamically the only apparent difference is in the stack gas losses. With flue gas recirculation, heat from the clean gas turbine exhaust can be extracted down to at least 100F below that of the HITAF exit gas.

Figure 1. Baseline HITAF System

Figure 2. Preliminary Heat Balance

The third arrangement (Fig. 4) would use air instead of gas turbine exhaust in the coal combustor. This approach is also being used in developing the HAT cycle schematic. The primary benefit that would accrue is a significant increase in flame temperature due to the increase in mol fraction of oxygen in the combustion air from 18.1% to 21%. The calculated adiabatic flame temperature goes from 3,925F to 4,343F with mixed combustion inlet air at 865F. This translates to a 43% increase in the radiant heat transfer potential. The effect on airside exit temperature is an increase of 53K (95F) in exit temperature over the base case in the REI simulation. There appears to be quite a bit of margin or safety factor to be gained from this approach.

On the downside, the inlet air must be preheated to a relatively high level to match that achieved using gas turbine exhaust. Flame temperature changes about one for one with combustion air temperature so there is a capital cost associated with the better radiant section performance. While there is additional stack gas, combustion air flow is reduced by 44 lb/sec.

Assuming that gas turbine exhaust can be cooled to 200F and HITAF exhaust to 300F, the following stack losses (above 70F) can be calculated:

GT exhaust fired - No flue gas recirculation - Loss = 58,477 Btu/sec
GT exhaust fired - With flue gas recirculation - Loss = 42,752 Btu/sec
Air fired - With flue gas recirculation - Loss = 49,875 Btu/sec

Clearly there is no big difference. The maximum difference is 15,700 Btu/sec which is less than 5% of the steam cycle input. This would result in a 2.5% reduction in output or a 1.2 point change in cycle efficiency.

The assumptions and conditions to be used for future combined-cycle analysis have been finalized and are presented in Table 1. These conditions are consistent with those used in other analyses of gas turbine-based power cycles, thereby giving a basis of comparison among various programs.

In finalizing the schematic, we should continue to use regeneration such as in the methane fuel supply and the coal transport air. Definite temperature limits for coal transport air as well as stack gas will be established in the next reporting period.

HAT Cycle

The HAT Cycle (Fig. 5) uses heat from within the gas turbine cycle to humidify compressor discharge air prior to its introduction into the gas turbine combustor. Thus, the mass flow and the specific work function of the mixture passing through the turbine are increased at essentially no penalty to the cycle. (Only the pumping power to bring the water for humidification to the correct operating pressure plus some duct losses.) This results in significantly increased power and higher efficiency. The HAT Cycle has the potential of performing at efficiencies equivalent to combined-cycle systems without the need for heat recovery steam generators and steam turbines. Preliminary results indicate efficiencies of over 50%.

The assumptions and conditions to be used for future HAT-cycle analysis will be finalized in the next reporting period. These conditions will be consistent with those used in other analyses of gas turbine-based power cycles, thereby giving a basis of comparison among various programs.

Figure 3. HITAF System with GT Exhaust Quench

Figure 4. HITAF System with Air Preheater

Figure 5. HITAF HAT Recuperative Cycle with Preheater

Table 1 - DESIGN CRITERIA FOR BALANCE OF PLANT EQUIPMENT

COMBINED CYCLE

HEAT RECOVERY STEAM GENERATOR

HEAT LOSS:

G.T. EXHAUST SIDE	1%
STEAM SIDE -SUPERHEATED STEAM - TEMPERATURE DROP)	7 F
STEAM SIDE (REHEATED STEAM - TEMPERATURE DROP)	5 F

PRESSURE DROP:

G.T. EXHAUST SIDE (NON—REHEAT STEAM TURBINE)	12 IN H2O
G.T. EXHAUST SIDE (REHEAT STEAM TURBINE)	16 IN H2O
SNCR	2 IN H2O

HIGH PRESSURE SUPERHEATED STEAM (PIPE LOSSES)	6.3%
INTERMEDIATE PRESSURE SUPERHEATED STEAM (PIPE LOSSES)	3.3%
REHEATED STEAM (PIPE LOSSES)	3.3%
STEAM TO REHEATER (PIPE LOSSES)	3.3%

HIGH PRESSURE EVAPORATOR (PIPE LOSSES)	1.8%
INTERMEDIATE PRESSURE EVAPORATOR (PIPE LOSSES)	1.8%

HIGH PRESSURE SUPERHEATER COIL	3.8%
REHEATER COIL	5.0%
INTERMEDIATE PRESSURE SUPERHEATER COIL	3.1%
LOW PRESSURE SUPERHEATER COIL	20 PSI

SUPERHEATED STEAM TEMPERATURES*:

HIGH PRESSURE STEAM (SEE BELOW FOR PINCH RESTRICTIONS)	900 TO 1100 F
REHEATED STEAM (SEE BELOW FOR PINCH RESTRICTIONS)	900 TO 1100 F
INTERMEDIATE PRESSURE STEAM	500 F
LOW PRESSURE STEAM	345 F

PINCH TEMPERATURES:

HIGH PRESSURE SUPERHEATED STEAM	MINIMUM 75 F
REHEATED STEAM	MINIMUM 75 F

HIGH PRESSURE EVAPORATOR	15 F
INTERMEDIATE PRESSURE EVAPORATOR	15 F
LOW PRESSURE EVAPORATOR	20 F

BOILER FEEDWATER TEMPERATURE APPROACH TO STEAM DRUM TEMPERATURE:

HIGH PRESSURE BOILER FEEDWATER	15 F
INTERMEDIATE PRESSURE BOILER FEEDWATER	15 F
LOW PRESSURE BOILER FEEDWATER/INTEGRAL DEAERATOR	20 F

BOILER FEEDWATER PRESSURE AT PUMP DISCHARGE:

HP BOILER FEEDWATER TO STEAM DRUM PRESSURE RATIO	1.415
IP BOILER FEEDWATER TO STEAM DRUM PRESSURE RATIO	1.304
LOW PRESSURE BOILER FEEDWATER TO DEAERATOR	+20 PSI

EVAPORATORS:

BLOWDOWN (PERCENTAGE OF STEAM PRODUCED)	1.0%
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TRANSITION DUCT FROM G.T. TO HEAT RECOVERY UNIT:

HEAT LOSS (TEMPERATURE DROP)	2 F
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SURFACE CONDENSER

HOT SIDE PINCH TEMPERATURE	10 F
SUBCOOLING	4 F

Table 1 (Continued)

<u>DEAERATOR</u>	
OPERATING PRESSURE	90 PSIA
VENT	500 LB/HR
<u>HEAT EXCHANGERS</u>	
FUEL GAS HEATER:	
HOT SIDE PINCH	20 DEG F
EFFECTIVENESS	90%
INTERCOOLER:	
COLD SIDE PINCH	20 DEG F
EFFECTIVENESS	90%
<u>BLOWDOWN FLASH DRUM</u>	
OPERATING PRESSURE	95 PSIA
*ASSUMING GAS TURBINE EXHAUST TEMPERATURE > = 975 DEG F	

TASK 3.1.1 - CHEMICAL REACTOR MODELING

TASK 3.1.1.1 - ENGINEERING ANALYSIS

In order to compare the performance of the two different designs being considered, *i.e.*, the long, axial flame design and the hot, rich, well-mixed design, detailed calculations were required addressing specific questions for each of the designs. In particular for the well-mixed, hot, rich design, information was required on just how hot and how rich the combustor needed to be in order to provide effective NO_x control. Subsequently, kinetic and equilibrium calculations were performed using the CHEMKIN gas-phase kinetics program developed at Sandia National Laboratories and the chemical equilibrium code developed at the NASA Lewis Research Center. Initial equilibrium calculations were performed using a Utah bituminous coal (Blind Canyon) to determine the optimum temperatures and stoichiometries for the minimum production of total fixed nitrogen (TFN).

The equilibrium variation of TFN production at adiabatic conditions is shown in Figure 6, where it can be seen that a minimum in TFN production exists at an

Figure 6. Total Fixed Nitrogen (TFN) predicted at equilibrium under adiabatic conditions for a Utah bituminous coal (Blind Canyon).

overall stoichiometry near 0.60. The TFN production at the higher stoichiometries (0.6 - 1.0) is due chiefly to NO formation, and the TFN at the lower stoichiometries (< 0.6) is due mainly to HCN and NH₃ formation.

Although the level of TFN production is very low at a stoichiometry of 0.6, the consequence of operating at this stoichiometry would be a considerably lower flame temperature. In the previously quarterly (Figure 17 of the report covering July 1 - September 30, 1992), a comparison was made showing adiabatic flame temperature as a function of stoichiometry. In that comparison, the adiabatic flame temperature dropped approximately 500 K (900 F) between a stoichiometric ratio of 1.0 and a stoichiometric ratio of 0.6. The implications are several. First of all, to approach the level of TFN shown at the minimum in Figure 6, the temperature has to be high enough such that the kinetics approach equilibrium; however, with the lower flame temperature, it is not clear how rapidly equilibrium would be reached. In addition, achieving the desired outlet temperature for the working fluid in the radiant heat exchanger is a critical design issue. It is necessary to maintain substantial temperatures within the HITAF to allow for the attainment of the desired level of heat transfer to the radiant panels. If the stoichiometry is lowered significantly to allow for a minimum production of TFN, then there is a strong possibility of not being able to achieve design goals at the outlet of the radiant heat exchanger; therefore, some concessions in terms of NO_x production may have to be made in order to meet other design constraints.

In order to access the kinetic aspects of this situation, CHEMKIN calculations were performed at various temperatures to determine how rapidly the equilibrium levels of TFN could be reached. The equilibrium combustion products of a Utah bituminous coal burned in preheated, vitiated air at a stoichiometry of 0.6 were used with the addition of 600 ppm of HCN to allow measurement of the rate of decay for the fixed nitrogen species. The calculations were performed over the temperature range of 1500 - 1900 K. The results, shown in Figure 7, indicate that at temperatures much lower than the adiabatic flame temperature for these conditions (~1800 K), the kinetics for conversion of TFN to N₂ are not rapid enough to be of use as "in-furnace" NO_x control. Further examination of Figure 7 indicates that perhaps even heat addition would be necessary to obtain the temperatures necessary (~1900 K) for reaction times that would be practical for the hot, rich combustor (residence time < 1.0 sec.). If the dimensions of the hot, rich combustor were increased to allow for greater residence times, then lower temperatures (near adiabatic conditions) could be used; however, increasing the size of the hot, rich combustor would be defeating one of the inherent advantages of this design.

An additional complication arises when considering the equilibrium effects of TFN production at higher temperatures. Equilibrium calculations were performed over a series of fixed temperatures using the Utah bituminous coal at a fixed stoichiometry of 0.60, and the results are shown in Figure 8. As is shown, the minimum production of TFN occurs near the adiabatic flame temperature and temperatures higher than the adiabatic temperature result in increasingly higher levels of TFN production (due chiefly to NO formation). Therefore, utilizing heat addition will help increase the kinetic rate of TFN decay; however, it will result in ultimately higher equilibrium levels of TFN.

Figure 7. Kinetic calculations of TFN decay. Calculations used equilibrium reaction products from the combustion of a Utah bituminous coal at an overall stoichiometry of 0.6, with an additional 600 ppm of HCN added.

Figure 8. Equilibrium calculations of TFN production as a function of temperature for a Utah bituminous coal at a fixed stoichiometry of 0.6.

TASK 3.1.1.2 - PARAMETRIC STUDIES

The experimental effort to provide validation data for the combustor modeling and evaluation data for the NO_x control concepts will be carried out in the 100,000 Btu/hr combustion research facility at the University of Utah. Extensive modifications of the facility have been recently completed which will allow simulation of various aspects of the HITAF system. The facility, shown in Figure 9, has a "U" configuration and is a down-fired facility. There are both natural gas and electric preheaters which allow

Figure 9. Schematic of the 100,000 Btu/hr experimental test facility at the University of Utah. The down-fired burner is on the left of the "U"-shaped design, and the isothermal test section is on the right side side of the "U". The gas-fired preheater is shown above on the third floor.

variation in the degree of vitiation and in the level of preheat of the combustion air. The burner is located at the top of the left side of the "U"-shaped furnace in the drawing and has a long vertical path over which axially-staged flames can be tested. The test facility is equipped with a multitude of access ports over the entire length of the combustion chamber, allowing testing of numerous reburning configurations as well. Also, the large single section on the right, or outlet side of the "U" facility is fitted with auxiliary burners in isolated combustion chambers allowing for the maintenance of an isothermal reaction zone for SNCR testing.

The research plan for testing the naturally-staged flame (long, axial flame concept) will consider a number of parameters. Initially, the desirability of vitiated combustion air will be investigated using a reduced inlet oxygen concentration as compared to standard air with various levels of preheat. In addition, alternatives for flame control will be investigated to obtain an optimum design for minimizing primary air and subsequently

minimizing NO_x formation during the natural staging of the flame. Experimental testing will include variations in the primary velocity of the primary air, flame anchoring techniques, particle-size distribution, and tests with distributed air. Although the naturally-staged flame utilizes the aerodynamics of the system to gradually mix in the combustion air, thereby obtaining the naturally-staged flame, there may be some advantage in introducing some of the combustion air at various downstream locations. The multiple access ports throughout the length of the "U"-shaped furnace will facilitate such testing.

Further experimental research will focus on the optimized application of reburning. Tests will be performed to identify the optimum HITAF reburning parameters and will include fuel type, injection temperature, and stoichiometry. Again, the multiple access ports of the "U"-shaped furnace will allow injection of either coal or natural gas at any desired location in the combustion chamber facilitating investigation of the impact of injection temperature and stoichiometry on the effectiveness of the reburning process.

The optimized application of selective non-catalytic reduction (SNCR) for NO_x control can be investigated in the isothermal zone of the combustor. Various issues will be addressed including the possibility of adding SNCR reagent with cold dilution air and the possibility of using the reburning zone to enhance reduction. The experimental tests planned include addition of SNCR reagent with large amounts of cold air, varying the injection temperature of the reagent, coupling the SNCR with reburning chemistry, evaluating the stoichiometry which may be best suited for the reagent, and varying the residence time for the rich zone.

The experimental testing will be accomplished with simultaneous combustion simulations of the "U"-shaped test facility to facilitate experimental planning as well as analysis of the experimental data. The intent is to utilize the modeling tools to identify key parameters to investigate experimentally, and to utilize the experimental results to provide confidence in the model predictions. The validated combustion model will then provide the means of scaling the experimental results to larger systems.

TASK 3.1.2 AIR HEATER DESIGN

Several refinements have been made to the designs which are being studied for use as radiant and convective air heaters to reflect the latest thinking as to requirements for air side pressure drops, heat transfer, sootblowing or ash removal, and materials of construction. Also, since equipment requirements for flue gas recirculation and clean-up are directly affected by the flue gas flowrate, a parametric study was made of the effect of hot gas flowrate on the size of the convective air heater.

Radiant Air Heater

The effective transfer of heat through the radiant air heater to the working air stream is integrally dependent on the construction of the air heater, the air side heat convection coefficient, and the air side pressure drop. Clearly, the air passages within the radiant air heater should be in relatively close proximity to the incident heat flux so as to minimize heat conduction path lengths. Moreover, the size and number of air passages must be adequate to provide effective air side heat convection at a reasonable air pressure drop and the face area of the radiant heater must be adequate to provide the required heat transfer rate. A section view of such a radiant air heater is shown in Fig. 10. As discussed in the previous report, a protective layer of refractory would be provided on the fire side of the radiant air heater to prevent

corrosion of the air passages by ash and slag which inevitably will deposit on the heater face. Accordingly, there exists a trade-off between the width, W , and length, L , of the radiant panel face, the air passage width, a , and height, b , and the resultant air side pressure drop and heat convection coefficient.

The results of an analytical study of the trade-off between the air side heat convection coefficient and pressure drop for the radiant air heater are shown in Figs. 11 and 12 for some typical dimensions. The effective heat convection coefficient shown in Fig. 12 accounts for the fractional increase in the convective surface area as a result of the secondary area of the fins and back surface of the passages, with an allowance for heat conduction losses. Relative to the resistances due to the refractory or the ash and slag layers, the effective heat convection coefficient should be of the order of 100 Btu/hr-ft²-F or greater to ensure that heat convection is not the major resistance. These figures show that for an allowable air pressure drop of 1%, the width and length of the radiant air heater face can be varied inversely to arrive at air passage dimensions which are reasonable and will achieve the required heat convection.

Convective Air Heater

There will inevitably be some deposition of ash onto heat transfer surfaces of the convective air heater from the hot flue gas. Even with the most effective ash rejection/removal concepts imaginable, one must be realistic and expect that, over extended periods of time, small ash particles, of the order of 1 μ m or less in size, will be deposited on the heat transfer surfaces. Therefore, sootblowing or other means will be required for periodic removal of ash deposits from heat transfer surfaces of the convective air heater. Accordingly, this requirement places certain constraints on the operating conditions and design of the convective air heater to ensure that ash removal can be accomplished effectively. First, the ash which is transported into the convective air heater by the flue gas must be "dry" as opposed to "wet" or "sticky." Second, since the ash deposit will impede the heat transfer process if it is allowed to buildup indefinitely, the ash cannot be allowed to sinter or densify on heat transfer surfaces to the point where it cannot be removed easily by sootblowing. Thus, to allow for effective sootblowing of ash deposits, the properties of ash in the flue gas and resultant deposits on hot surfaces must be controlled by constraining the flue gas temperature so that the ash isn't "sticky" or it doesn't densify on surfaces in a short period of time (e.g. in less than 8 to 12 hours). Based on an analysis of the properties and characteristics of ash deposits performed in Task 3.1.5, the maximum flue gas temperature at the convective air heater inlet is between 1800 and 2000 F for bituminous coals if the ash deposit thickness isn't allowed to exceed 1/2 inch. In addition, because of the eventual buildup of ash deposits on surfaces of the convective air heater, the surfaces must be separated sufficiently to avoid bridging of gas flow passages by the ash deposits as well as permit access for sootblowing equipment.

Since there are practical limits as to the amount of heat which can be transferred via the radiant air heater, additional cooling of the flue gas will be required to constrain the flue gas temperature at the inlet to the convective air heater so as to provide for effective removal of ash deposits. Based on theoretical modeling of the HITAF combustor under Task 3.3.1, a reasonable split between the radiant and convective air heaters is 47% and 53%, respectively. Thus, for a typical bituminous coal the hot gas temperature would be reduced from about 3000 F at the exit of the radiant air heater to 1800 F at inlet of the convective air heater. The mode of hot gas cooling could be some combination of direct cooling by water walls or steam superheating and gas quenching by gas turbine exhaust air or flue gas recirculation. While the direct cooling modes reduce the availability or quantity of thermal energy content in the hot gas stream, the quenching methods degrade only the quality but not the quantity of thermal energy content. Clearly, a determination of the best operating conditions, heating rates for the radiant and convective air heaters, and method for moderating the hot gas temperature at the convective air heater inlet will depend on the results of optimization studies which have yet to be performed.

Figure 10. Radiant Air Heater Section

Figure 11. Radiant Heater Air Side Pressure Drop
Furnace and Air Passage Dimensions

As guidance for optimization of the HITAF, a study was conducted of the effect of hot gas flowrate on the size of the convective air heater when the hot gas inlet temperature is constrained to 1800 F. The range of hot gas flowrates considered covers the extremes from all direct cooling to a combination of dilution or quenching and a small amount of cooling. The air was assumed to be heated from 732 F to 1300 F, which represents about 53% of the total heating from coal combustion. A staggered fin-tube-sheet arrangement was assumed; with 4"x 3", center-to-center, spacings between tubes in the directions perpendicular and parallel to the hot gas flow, respectively. The predictions in Figs. 13 and 14 clearly show that, when the hot gas inlet temperature is constrained to a fixed value of 1800 F, the size of the convective air heater increases significantly for hot gas flowrates less than about 300 lbm/sec. By inference, the cost of the convective air heater would increase accordingly. Conversely, for hot gas flowrates greater than about 300 lbm/sec, the size and cost of the convective air heater change only moderately. Therefore, these results serve as guidelines for providing the lowest possible size and cost for the convective air heater.

In view of the requirement for providing adequate spaces between surfaces of the convective air heater to avoid bridging of gas flow passages by ash deposits and permit access for sootblowing, a design study was performed to evaluate the effects of these provisions on the heater dimensions and weight. Although the required spacings have not yet been determined, the results of this study will be useful for identifying the trends associated with gross changes to the heat transfer configuration. The results of this study are summarized in the table below.

Figure 12. Radiant Heater Air Side Coefficient
Furnace and Air Passage Dimensions

Figure 13. Convective Air Heater Dimensions
Effect of Hot Gas Flowrate

Figure 14. Convective Air Heater Volume and Weight
Effect of Hot Gas Flowrate

TABLE 2

TUBE ARRANGEMENTS, SPACINGS, AND DIMENSIONS OF CONVECTIVE AIR HEATER

Two pass cross counterflow configuration.
 Two (2) units required per gas turbine.
 Hot gas inlet temperature 1300F Air outlet temperature
 410 lbm/sec Hot Gas 363 lbm/sec Air
 53% Effectiveness $C_{min}/C_{max} = 0.88$
 SiC 1/4 inch tube wall thickness
 2% pressure drop per side

<u>Heat Transfer Configuration</u>		<u>Dimensions (ft, ft³, lbm)</u>				
<u>Tube Arrangement/Spacings</u>		<u>Lc</u>	<u>Lh</u>	<u>Lnf</u>	<u>V</u>	<u>W</u>
Staggered	2" x 1.5"	5.4	2.6	78.7	1117	97,600
Staggered	2" x 2"	5.4	3.5	79.1	1521	99,600
Inline	2" x 1.5"	5.7	3.9	54.9	1230	107,500
Inline	2" x 2"	5.3	4.3	64.5	1475	96,700
Staggered	3" x 2.25"	14.4	4.2	29.3	1780	103,700
Staggered	3" x 3"	14.7	5.7	29.5	2463	107,600
Inline	3" x 2.25"	15.2	6.0	21.2	1928	112,400
Inline	3" x 3"	14.2	6.8	24.5	2358	103,000
Staggered	4" x 3"	25.4	6.0	16.8	2566	112,200
Staggered	4" x 4"	26.2	8.1	16.9	3590	117,700
Inline	4" x 3"	26.6	8.3	12.4	2732	119,400
Inline	4" x 4"	25.3	9.5	14.2	3410	111,800

Tube arrangements are either staggered or inline relative to the hot gas flow. Spacings are center-to-center distances between tubes in the directions perpendicular to and parallel to the hot gas flow, respectively. Lc, Lh, and Lnf are the lengths of the convective air heater relative to the cold (air) flow, the hot gas flow, and the no flow directions, respectively. V and W are the overall volume and weight of the convective air heater, respectively.

In general, larger spacings between tubes require that the convective air heater dimensions change as follows: (1) the cold and hot flow lengths increase, (2) the no flow length decreases, and (3) the overall volume increases accordingly. However, the weight of the air heater increases by a smaller percentage as tube spacings are increased. This can be explained by a requirement for increased surface area needed to compensate for a decrease in the heat convection coefficient on the hot gas side as tube spacing is increased. The choice of tube arrangement (i.e. staggered or inline) for minimum air heater dimensions and weight depends on whether the tube spacings are symmetrically or not. For fixed symmetrical tube spacings, the inline tube arrangement provides the smallest volume and weight, while for fixed asymmetrical tube spacings, the staggered tube arrangement gives the smallest volume and weight. However, for a given tube spacing perpendicular to the hot gas flow, asymmetrical tube spacings with a staggered arrangement provide the smallest volume and only slightly more weight than the comparable inline tube arrangement. Therefore, future design studies for the convective air heater will utilize the staggered tube arrangement with asymmetrical tube spacings so as to provide the most practical design.

A computational fluid dynamics (CFD) model is presently being prepared to more accurately determine the spacings required in the convective air heater to avoid excessive ash buildup. Since ash deposition on heater surfaces involves very complex phenomena which are difficult to predict, the CFD model will only provide guidelines for designing the convective air heater. Therefore, advice was sought from Diamond Power Specialty Co., a producer of sootblowing equipment. For the types of coals being considered for the HITAF program, tube spacings perpendicular to the hot gas flow are typically 3 to 6 inches. Until more precise information is

available from the CFD model, future design studies for the convective air heater will be based on a 4" center-to-center tube spacing, perpendicular to the hot gas flow.

Based the concepts discussed above for correct design of the convective air heater a heater was sized to provide the latest requirements of the cycle analysis. The thermal requirements of the convective air heater are tabulated below.

TABLE 3
THERMAL PERFORMANCE REQUIREMENTS OF THE CONVECTIVE AIR HEATER

For heat input rates: 35% from natural gas and 65% coal.

<u>Hot Gas Side</u>		<u>Cold Air Side</u>
630	Mass Flow	726
1800	Inlet Temperature (F)	732
1180	Outlet Temperature (F)	1300
17	Inlet Pressure (psia)	235
2	Pressure Drop (%)	2
	392 MBtu/hr Heating Rate	
	58% Effectiveness	

A conceptual drawing of a two-pass, cross-counter-flow, convective air heater for these thermal performance requirements is shown in Fig. 15. Two convective air heaters, each having a core weight of 143,000 lbm, would be required per gas turbine. This design is based on the finned-tube-sheet surface configuration which is expected to minimize circumferential thermal stresses in tube walls, enhance heat convection on the hot gas, and minimize ash deposition on heat transfer surfaces. Although this design was based on SiC for the finned-tube-sheet material, a high temperature metal alloy might be suitable for these conditions since the maximum tube wall temperature should not exceed 1430 F. With the metal alloy, the dimensions would be essentially the same as for SiC but the weight would increase by 69% for the alloy.

If the overall shape of the convective air heater shown in Fig. 15 is not suitable for some reason, the overall dimensions of the heater could be revised by adjusting the allowable pressure drops. For example, the air side tube lengths could be reduced by decreasing the air side pressure drop and/or increasing the hot gas side pressure drop. Changing the pressure drops for both sides by 50% to 1% for the air and 3% for the hot gas, respectively, would reduce the cold (air) flow length to 23.6 ft and increase the hot gas and no flow lengths to 10.0 ft and 14.3 ft, respectively, while increasing the core weight and volume by less than 3%.

Figure 15. Convective Air Heater. Configuration/Dimensions

Task 3.1.3 Air Heater Materials

The materials used on both the convective and radiant air heater sections must withstand both high operating temperatures and the corrosive and erosive effects of coal ash in combustion ambients. Activity during the reporting period was divided into three topic areas: 1.) Obtaining and providing test bar specimens of downselected ceramic materials to ORNL for baseline mechanical property assessment, 2.) an initial evaluation of alkali corrosion of selected ceramics at PSIT, and 3.) an initial evaluation of coal ash corrosion for both dense ceramics and refractories at UNDEERC.

Ceramics for Evaluation at ORNL

Preliminary Downselect of Engineering Ceramics for heat exchanger construction has been completed. Materials and vendors were assessed as to the probability of the material surviving coal ash ambients, as well as the ability of the vendor to scale up their production process for fabricating the large air heater panels for the HITAF. Ceramic materials assessed to date have included sintered silicon carbide, reaction sintered silicon carbide, reaction infiltrated alumina/silicon carbide, sintered alumina, and sintered silicon aluminum oxynitride (SiAlON). Earlier work by V. Tennery and M. Ferber at ORNL (1979-1984) evaluating ceramics in coal combustion environments indicated that sintered aluminum oxide and SiAlON ceramics degraded rapidly in both acidic and basic coal ash environments, due to strong chemical interaction between the ash and these types of ceramics. Based on this information, effort has focused in silicon carbide and alumina/silicon carbide materials as candidate materials.

Reaction Sintered Silicon Carbide, as well as sintered silicon carbide, appear to be ideal candidates based on available literature regarding the corrosion of ceramics. Norton Co.s (Worcester, MA) NC430 reaction sintered silicon carbide material was evaluated in tube form as part of a DOE funded indirect coal fired gas turbine project conducted in the early 1980's at Solar Turbines International, Inc. According to their report, the material survived operational temperatures up to 1200C in coal ash ambients for 1000 hours with no significant degradation. Norton Co. has since improved the strength of their reaction sintered silicon carbide, as well as reduced the residual silicon content, thus making it more attractive for application in HITAF. Accordingly, UTRC has obtained approximately 220 MIL STD 1942 "B" ceramic test specimens of NT-230, and supplied them to ORNL HMTL for evaluation. In addition, Norton Co. has provided gratis samples for alkali and coal ash corrosion testing.

Sintered Silicon Carbide (of near theoretical density) has also been of considerable interest for use in heat exchangers in corrosive environments. This material's lack of residual silicon content should give it superior corrosion resistance as compared to reaction sintered silicon carbide. Both Carborundum Co. (Niagara Falls, NY) and Coors Ceramics (Golden, CO) were contacted, as Carborundum is the sole U.S. supplier of alpha sintered silicon carbide, and Coors holds a licence from General Electric Corp. for production of beta sintered silicon carbide.

Carborundum's alpha silicon carbide (Hexalloy SA) has been well characterized as to its baseline mechanical properties within previous DOE Office of Transportation programs for ceramics applications in automotive engines. Since a sufficient database exists for the Carborundum sintered silicon carbide, UTRC has elected to obtain the Coors/GE sintered beta silicon carbide for mechanical testing at ORNL, as little is known about this material. However, its potential usefulness to the HITAF program is high based on

preliminary data. Orders have been placed for test panels of the material, and these should arrive during the next reporting period for machining into test bars.

A Silicon carbide/Aluminum/Alumina Matrix Particulate Composite material (Lanxide DIMOX) developed by DuPont/Lanxide has been receiving considerable attention by the heat exchanger community. Internal testing at DuPont's CONSOL test facility have demonstrated that these materials are reasonably resistant to coal combustor ambients, perhaps due to the residual aluminum metal in the material. Test panels were obtained and machined into 250 MIL STD 1942 B test bars. After inspection by Lanxide Corp., 210 bars have been forwarded to ORNL HMTL for evaluation. In addition, Lanxide had provided some gratis test coupons for ash soak testing at UNDEERC, and excess material from test bar machining has been provided to PSIT for alkali exposure testing.

Ash Soak Testing at UNDEERC

Samples of reaction sintered silicon carbide, sintered silicon carbide, and alumina/silicon carbide have had preliminary machining and await ash exposure at UNDEERC. After corrosion, the samples will be diced into test bars and undergo room temperature flexural testing at ORNL. Some preliminary results on sintered silicon carbide are reported in Section 3.2.3.

Alkali Exposure Testing at PSIT

Samples of sintered silicon carbide, reaction sintered silicon carbide, and alumina/silicon carbide are undergoing alkali exposure testing at PSIT.

Conclusion

Preliminary selection of materials for evaluation as potential structural convective and radiant air heater components is nearing completion. In conjunction with the samples being provided to ORNL for baseline (uncorroded) mechanical testing, preliminary corrosion testing of the chosen materials is being performed at PSIT and UNDEERC.

Task 3.1.4 Air Heater Structural Analysis

A variety of seals suitable for joining silicon-based ceramic structural components are under development as part of the Combustion 2000 program. The seals will be used at connections of ceramic heat exchanger tubes to ceramic or metal headers, tube-to-tube connections, and other regions in the coal-fired power generation system. The seals can be subjected to high pressure and high temperature. Minimal joint stresses, low leakage rates, and reasonably simple geometries are goals in the design of the seals.

Two important seal configurations are addressed in this report: the seal for a tube-to-tube connection, and the seal for a tube-to-header connection. Several baseline designs for both types are described, based on information found from the literature. Starting with these preliminary designs, future work will be directed toward further defining and developing seal design to obtain the optimal seal configurations.

Seals are necessary to limit gas leakage at the connection of structural components in a coal-fired power generation system. The seals must be able to withstand the harsh thermal environment present in the system, in addition to the high differential pressure across the seal.

Thermal expansion and the stresses due to the mismatch in component thermal expansion coefficients must be accounted for. The seals must be strong enough to survive the harsh conditions, compliant enough to allow for movement, yet allow for a minimum of leakage.

In this report, tube-to-tube and tube-to-header seals are considered, and specific conditions at the joints are specified. Some preliminary baseline seal designs are described based on a review of a literature survey on high temperature seals.

Seals are required in any location in which two components are joined and the prevention of gas leakage is important. Two specific locations that are important in the Combustion 2000 power system are the connection of two tubes and the connection of a tube to a header.

The range of temperature and pressure at these different locations will vary, as will the component materials. For example, the inlet manifold will be pressurized, but may be at a much cooler temperature than the outlet manifold. Also, the inlet manifold may consist of a ceramic tube entering a hole in a metal header plate, whereas the outlet manifold could be composed of a ceramic tube connected to a ceramic header plate. In addition, the heat exchanger geometry could vary inside the same power system, so that individual tubes may be used in one application, and sheets with internal passages which act like tubes may be used in a different application. For the preliminary study in this report, it will be assumed that the baseline condition is individual tubes.

Definition Of Baseline Configurations

In an effort to define parameters for this preliminary design study, two baseline configurations are defined. For the tube-to-tube seal, it is assumed that the tubes are both constructed of the same ceramic material. The pressure differential through the wall thickness is 100 psi, and the maximum temperature is 2000 F. For the tube-to-header seal, the geometry is assumed to be a ceramic tube which enters a hole in a ceramic header plate. A maximum temperature of 1800 F and a maximum differential pressure of 230 psi are specified as the baseline conditions.

The objective of this study is to develop a preliminary seal design which can withstand these conditions without failure, can be compliant enough to allow for displacement due to thermal expansion of the components, and can limit the amount of gas leakage across the seal.

The consensus of the literature surveyed is that seal development for high-temperature ceramic heat exchangers remains an unresolved problem. However, two reports in particular contain interesting and useful information. In the report by Ward, et al (1980), relaxing joints and permanent joints are presented which show great promise for the high-temperature and high-pressure applications in the Combustion 2000 program. A report by Strumpf et al (1985) discusses high-temperature compliant seals using ceramic fiber and refractory mortar.

Tube-To-Tube Seals

Three approaches to the tube-to-tube seal design have been identified by Ward, et. al (1980) and are listed below. Each of the three designs will be briefly described.

1. Glass-filled relaxing seal
2. Silicon brazed permanent seal
3. Spherical compression seal

Figure 16. Sketch of Relaxing Tube Joint

Figure 17. Spherical Compression Joint

The relaxing joint seal consists of concentric tube elements with a glass fill in the interface, shown in Figure 16. The glass exhibits viscous properties which allow the tubes to slide relative to each other longitudinally while maintaining a gas-tight seal. The most important factor in obtaining a successful design is selecting a glass adhesive that has the appropriate specific viscosity versus temperature characteristics. For example, joints will occur at different locations in the heat exchangers, and will therefore exist at different temperatures. The desired viscosity must exist over a large range of temperature to be successful.

The glass adhesives must remain structurally stable during long term, high-temperature service, maintain desirable viscosity characteristics, and resist damaging reactions. Corning 7800 was selected from a series of glasses after testing as the best choice for a glass adhesive.

Relaxing seals were tested at pressures of 100 psi and temperatures up to 1400 F and leakage rates were found to be negligible after 1000 hours. However, due to the lack of testing and time constraints in the development program described in Ward, et. al (1980), the glass-filled relaxing seal was relegated to a back-up system.

The geometry of this joint seal is similar to the glass-filled relaxing seal, shown in Figure 16, i.e. a tube and sleeve joint. In this case, silicon is used as the bonding agent instead of glass. This seal was found to be impermeable, and contained shear strengths as great or greater than the silicon carbide heat exchanger tubes.

This particular seal design does not compensate for thermal expansion, and additional means must be found to allow for expansion in other locations, such as attaching the cold end of the tube to a metal bellows (Ward, et. al, 1980, Ward, et. al, 1983).

This particular seal design is applicable to both tube-to-tube joints and tube-to-header joints. The configuration in Figure 17 represents a spherical compression joint. The geometry consists of a spherical seat on which rests an appropriately shaped tube end. The basic premise is that if the contacting surfaces are constructed to a given tolerance, and the joint is kept in compression, a seal will occur which will minimize the leakage at the joint.

Testing between a ceramic-to-ceramic spherical joint indicated that the joint was pressure checked to 80 psi without any leaks by applying a moderate compressive load of 33 pounds.

A practical method of assuring compression at the joint is again to incorporate a metal bellows at the cold end of the heat exchanger tube. This requires a ceramic-to-metal spherical compression joint at the connection to the bellows. Proper tooling and coating the metal seat with a high temperature coating increase the effectiveness of this type of joint. The ceramic-to-metal joint was tested at 80 psi and 1300 F for 25 hours with a cyclic deflection load of 4.85 pounds once each hour, and the joint did not leak with a 44 pound compression load.

Two possible problems with this type of joint are buckling due to the compression load, and differential expansion between tubes connected to the same header. For the magnitude of compressive loading under consideration, buckling is not a problem. The differential expansion problem will be discussed under the tube-to-header section of this report.

Tube-To-Header Seals

Four different general seal designs used to prevent gases from leaking through a joint at which a tube enters a header are listed below. Each of these designs will be briefly described.

1. Spherical Compression Seal
2. Fiber Tube Seal
3. Cemented Tube Seal
4. Fiber Layer Seal

This seal geometry was described in the previous section for tube-to-tube seals. However, instead of two tubes being joined in this manner, the header could contain a sleeve which connects to the tube. The concept is exactly the same as before.

A concern with this type of joint seal is the differential expansion between tubes connected to the same header. Since the tubes will be at different temperatures, the cooler tubes will contract, possibly causing a gap to form in the spherical joint. Analysis has shown that this leakage could exceed five percent of the flow, which is unacceptable.

The solution to this problem is to allow each tube to expand separately. This can be accomplished using a metal bellows at the cold end of the tube.

Figure 18 contains a schematic drawing of a ceramic fiber wrap seal (Strumpf, et. al, 1985). The major features of this seal are a ceramic fiber compliant layer located between the tube flange and the header plate, and ceramic fibers wound around the section of the tube that extends through the header plate.

The compliant ceramic fiber is included to allow for expansion of the joint. This layer was constructed using fiber material such as Cerafiber paper, Durablanket and 880-J paper. In an initial test considering only the compliant fiber layer at room temperature and at pressures under 1 psi, excessive leakage rates were measured using an applied sealing force of 6.9 pounds.

Since an adequate seal could not be formed at the tube-header interface, ceramic fiber rope, yarn, roving, cord or string was introduced into the seal design by wrapping the tube where the tube goes through the header plate hole. Examples of the types of material for this application include Fiberfrax rope or cloth and Nextel fiber. Room temperature tests concluded that leakage rate was greatly reduced, but a press fit of 50 pounds was necessary to force the fiber-wound tube into the header plate hole.

High-temperature tests were conducted to evaluate the tube-to-header seal. Test conditions included temperatures up to 2400 F and 2 psi differential pressure. Results were inconclusive, although excessive leakage rates, and crushed and brittle fibers were found.

An alternate approach to the fiber tube seal is the cemented tube seal. The cemented tube seal has the same geometry as in the fiber tube seal, shown in Figure 18, except that the wrapped fiber on the tube is replaced with a cemented seal. Phosphate-bonded alumina cements were used as the cement material.

Tests performed on this configuration indicate very low leakage rates for temperatures up to 2450 F and a pressure differential of 1 psi. Strain relaxation was observed in the cemented joint.

Figure 18. Schematic of the Ceramic Fiber Wrap Seal

Figure 19. Fiber Layer Seal

This approach to designing a tube-to-header seal is similar to the two previous methods in that a ceramic fiber layer is used to seal the joint in addition to providing a compliant joint. In this configuration, shown in Figure 19, the ceramic fiber layer is on the inside of the header plate, and the pressure inside the header is used to press down on a plate which in turn compresses the ceramic fiber layer. This approach was not found in the literature; the design has been discussed by Combustion 2000 team members as a possible sealing configuration.

This type of seal requires high pressure to provide for the compression of the fibrous ceramic layer. Possible improvements to the design are changing the header plate thickness near the tube hole so that the ceramic fiber is forced into the interface, adding length to the sealing surface using a flange on the inside or outside of the header plate, and rippling the tube surface to provide for better fiber fill along the interface.

The literature survey has shown that very little research has been done in the temperature and pressure ranges that are of interest for the HITAF program. However, using existing data, seal designs can be extended to operate in the higher regimes.

Regarding the tube-to-tube joints, all three seal types have certain advantages and disadvantages. If a permanent seal, such as the brazed silicon seal or the spherical compression seal, are used, then thermal expansion of the heat exchanger tubes must be accounted for at the headers. The relaxed joint seal would provide for more compliance, but testing and analysis would be required to determine if this type of seal could be used in the current application.

The spherical compression seal requires that the tube be kept in compression at all times, which seems to indicate the need to include metal bellows as a method of achieving this condition. However, if a compliant header seal can be developed, the simplest method for joining tubes together is most likely the brazed silicon seal, assuming that this type of seal can withstand the pressure and temperature constraints.

Regarding the tube-to-header seals, the fiber tube seal and the cemented tube seal are both marginal at a pressure differential of 1 psi. Since this application has a design pressure differential of 230 psi, confidence is not high that either of these approaches will succeed. The spherical compression seal is a possible solution, but the lengths of the tubes that may be encountered in this application could make that type of seal prohibited.

The fiber layer seals are untested at this point, although previous tests with similar approaches at low pressure are not encouraging. However, assuming increased material property development in the ceramic fiber industry during the past several years, and better performance due to the high pressures, this approach contains the most potential for success.

The leading candidates for seal design will be evaluated for application in this heat exchanger system. This preliminary study will be accomplished using the following steps:

1. Define a particular geometry (or geometries), with dimensions and maximum design pressures and temperatures. A uniform temperature field will be assumed. Maximum displacement due to thermal expansion must also be estimated.
2. Analyses of stress in the joint due to thermal and mechanical loads can be calculated. Probability of failure of the ceramic components will be determined. Models for analyzing changes in density for materials and effects such as relaxation can be included.

3. Tests for measuring leakage rates can be designed. It may be possible to develop an analytic model for leakage, although leakage data must exist or be obtained for a particular fiber ceramic material.
4. After preliminary analysis and/or test, an overall sealing design will be selected and finer detailed analysis will be initiated.

Task 3.1.5. Definition/Evaluation of Slag Impacts

Conditions in both the radiant and convective air heaters were investigated this quarter in order to understand the nature of the deposits and to identify the best means for removing deposits. Sootblowing conditions in conventional power plants were explored to determine if this could be used in the radiant section. Furnace calculations indicate that the walls of the radiant panels will be hot enough to allow flowing slag. Therefore, a calculation of the expected slag layer thickness was made. In the convective air heater, the main concerns were rapid sintering of ash deposits that would make sootblowing impossible. Calculation of the densification of the deposit as a function of time was made in order to determine the maximum allowable flue gas inlet temperature.

Ash Deposit Properties

We have picked four coals for detailed analysis in Combustion 2000. These coals represent a wide range of American coals as discussed in Quarterly Report 2.

Using PSIT's algorithms for calculating ash viscosity, we can examine the tendency of deposits to densify and/or flow on the radiant air heater surface. Using the ASTM ash composition, the viscosity was calculated as a function of temperature from 1000 K to 1900 K. The results of these calculations will be used to estimate the flowing slag layer thickness and deposit densification rates discussed below.

Figure 20 displays the viscosity-temperature curves for oxidizing (all iron as Fe^{+3}) conditions. Raask (1985) describes the behavior of deposits on surfaces as a function of viscosity. These observations have been adapted to the program coals in Table 4. Under oxidizing conditions, the onset of sintering is approximately 1000K (1350F) for the PRB coal and 1200 K (1700F) for the bituminous coals. Formation of "flowing" slag begins at about 1400 K (2060F) for the PRB coal and about 1650K (2500F) for the bituminous coals.

Radiant Air Heater

In pc-fired power plants, sootblowers are used in the furnace and in the convective passes to remove ash. Furnace sootblowers are typically retractable, short lance blowers (called wall blowers) which penetrate the furnace by 1 to 2 inches. Superheated steam or air at high pressures is directed at the wall from the nozzle tip. The angle of the jet with respect to the deposit surface is typically 30 degrees. The exit velocity from the nozzle is supersonic. The nozzle rotates through 360 degrees and can cover about a 5 foot radius. A blowing frequency of 4 to 8 hours is common. In the convective section, a retractable lance is used. The tip has two opposed nozzles. While the lance traverses a path between tubes, the tip rotates. Deposit removal is from a combination of thermal and mechanical shock.

Figure 20. Viscosity Based on Bulk Ash Composition

Table 4. Combustion 2000: Expected Ash Sintering Behavior
(Based on Raask, Table 9.1) Under Oxidizing Conditions

	log mu [Poise]	Temperature [K]			
		Wyodak	Blind Canyon	Illinois 6	Cedar Grove
On Surfaces:					
Onset of Sintering	10-12	1000 - 1030	1120 - 1200	1120 - 1215	1120 - 1220
Medium-Rate Sintering	8-10	1030 - 1060	1200 - 1300	1215 - 1320	1220 - 1330
Rapid Sintering	6-8	1060 - 1140	1300 - 1400	1320 - 1450	1330 - 1480
Formation of Non-Flowing Slag	4-5	1220 - 1360	1460 - 1550	1510 - 1600	1550 - 1650
Slow Movement by Gravity	3-4	1360 - 1460	1550 - 1660	1600 - 1630	1650 - 1690
Flowing Slag in Cyclone-Fired Boilers	2.4	1475	1755	1790	1820

Either superheated steam or compressed air are used; the choice depends on plant economics. Typical input (i.e., pre-nozzle) temperatures are 500 to 700F (260 to 370C) using steam and 600F (315C) using secondary air. Flow through the soot blower nozzles is supersonic. For coals which have deposits that are difficult to remove, e.g., sub-bituminous coals, water is sometimes used instead of air or steam. Retractable lances are used to apply water in much the same way as air or steam. The latent heat of vaporization of the water is used to induce a temperature drop in the deposit.

The long, axial flame can be assumed to have oxidizing conditions at the wall because of the slow radial mixing along the flame. Using the wall temperatures reported in the second quarterly report, we expect the surface to be 1500 to 1800 K (2250 to 2800F). At these wall temperatures, we can expect that none of the coals in this study are removable by conventional sootblowing because the ash deposits will be heavily sintered or flowing and because sootblowing may remove the refractory underneath the ash deposit as well. The PRB coal will flow down the wall. The bituminous coals will probably have some flowing component, although the slag layer will be thicker and there may be a "frozen" layer underneath the flowing slag.

In order to quantify the thickness of a flowing slag layer, a simple calculation was performed. Based on the analysis of laminar flow (Bird et al, 1960), the layer thickness is related to the flow rate and viscosity. The layer thickness was calculated for the counterflow case ($T_{250} = 2300\text{F}$) and Wyodak coal. The ash flux was estimated to be $8.2 \times 10^{-6} \text{ kg/m}^2\text{-s}$. Figure 21 shows the temperature and layer thickness as a function of length. Due to the flow patterns in the flame, there is very little deposition on the walls. Consequently, the slag layer is only 1.1 mm (0.04 inches) at 20 meters length.

The bituminous coals have higher viscosities than Wyodak and higher deposition rates since they have at least twice the ash content. An estimate of the behavior of Illinois 6 was made, shown in Figure 22, by doubling the mass flux via deposition and substituting the appropriate viscosities. Note that by 20 meters in length, the viscosity of Illinois 6 is predicted to be 2×10^4 poise, just barely sufficient for gravity flow according to Table 4. The layer thickness increases rapidly after about 10m in length as the surface temperature decreases. After 20 meters, the layer thickness is predicted to be 5.5 mm (0.22 inches). The Blind Canyon and Cedar Grove coals should behave in a similar manner under oxidizing conditions.

At the predicted wall temperatures, we can expect that none of the coals in this study are removable by conventional sootblowing. The PRB coal will be expected to flow down the wall. The bituminous coals will probably have some flowing component, although the slag layer thickness will be thicker and there may be a "frozen" layer underneath the flowing slag.

Convective Air Heater

What are the deposit characteristics that relate to successful deposit removal? This question was addressed by Wain et al (1992). They measured thermal and mechanical properties of ten slags and tried to relate the measurements to sootblowing experience.

Figure 21. Slag Layer Thickness for Wyodak Counter-Flow Case

Figure 22. Slag Layer Thickness for Illinois 6 Counter-Flow Case

The compressive strength was measured as a function of porosity. As porosity decreased, the compressive strength increased. The change in compressive strength was most dramatic as the porosity dropped below 25%. Above 25% porosity, all the slags had similar compressive strengths. The elastic modulus showed similar trend with porosity. The authors state that "This value of the porosity may be considered a *critical porosity* level, since above 25 vol% the mechanical properties can be predicted with a fair degree of accuracy, regardless of the composition or microstructural details."

It is well known that reduction in porosity, i.e., sintering of the deposit, makes the deposit harder to remove by sootblowing. Since the deposit strength is fairly constant for porosity greater than 0.25 (vol% = 25), we can conclude that this is a critical porosity for deposit removal by sootblowing.

Experience in coal-fired power plants says that the temperature entering the convective pass for "dry" furnaces is in the range of 2000 to 2600_F, depending on the coal type and heat flux. Higher temperatures would presumably cause problems with deposit removability in the convective air heater.

The most important criterion for determination of the maximum allowable gas temperature entering the convective air heater is that the deposit must be removable by sootblowing. Since an ash deposit has a finite thickness, we can specify that the "outer" surface of the deposit must not be so sintered as to prevent easy removal by sootblowing. The criterion for easy removal will be that a porosity of 0.25 or more will be obtained in greater than eight hours. Eight hours was picked as a convenient time for sootblowing.

Densification calculations were performed using the method of MacKenzie and Shuttleworth (1949). The bulk ash composition was used to calculate the viscosity. The initial deposit porosity was assumed to be 50% and the mean pore radius of 10 microns was assumed. Figure 23 shows a densification plot for Illinois 6 coal. The point at which $t = 3 \times 10^4$ s and a relative density equals 0.75 corresponds to a temperature of approximately 1180 K as shown on the figure.

Calculating the gas temperature from a knowledge of the deposit surface temperature requires a knowledge of the deposit thickness. Since ash is a good insulator (particularly if it is not sintered), the temperature drop across the ash must be accounted for. The gas temperature can be calculated by solving the steady state heat transfer problem given the air and deposit surface temperatures. This involves solving three equations for three unknowns.

The results are shown in Figure 24 for four coals. For ash layer thickness less than 6.4×10^{-3} m (0.25 inches), the allowable flue gas inlet temperature is very sensitive to deposit thickness. If the SiC surface is "clean", the inlet gas temperature can be high, 1500 to 1750 K (2200 to 2700_F), for the bituminous coals.

This analysis also serves to remind that the ash layer thickness is part of the convective air heater design. We must decide what amount of ash we can tolerate in the convective air heater in order to maintain adequate heat transfer. These calculations are rather simplistic since they just look at steady state heat transfer at one point in the convective air heater (i.e., the hottest point). However, when coupled with a design for the convective air heater that includes limits on the ash layer thickness, they can be used to set the maximum inlet gas temperature.

Figure 23. Densification of Deposit Illinois 6 (EPRI TAG)

Figure 24. Maximum Flue Gas Inlet Temperature to Convective Air Heater

Task 3.2.1. Coal and Ash Preparation and Analysis

Analysis of the first two coals to be used for producing slag for the ceramic corrosion tests were completed. They include a Wyodak seam coal from the Rochelle mine, Wyoming and an Illinois No. 6 from the Burning Star mine, Illinois. The majority of the analyses were reported Quarterly No. 2. Generation of ash from Illinois 6 coal for corrosion testing was completed. Once again, size segregated ash was generated as well as total ash samples.

Task 3.2.2. Mineral/Ash Transformations

The first series of alkali corrosion experiments was initiated this quarter. SiC substrates, both siliconized silicon carbide and sintered silicon carbide, were received from UTRC. The test coupons were coated with sodium sulfate and/or ash and soaked at 1173 K in the presence of SO₂, sodium sulfate and water vapor for eight days (200 hours). The samples were cross-sectioned at EERC and analyzed by SEM.

Prior to testing, each coupon was washed with Versa Clean (Fisher), rinsed with distilled water, acetone, and methanol. In some instances, a thin layer of sodium sulfate was applied to the surface using an airbrush. A target loading of 5 mg/cm² was achieved by weighing the coupons before and after application. Some samples were coated with approximately 3 mg of Wyodak ash (< 3 μ m). Sodium sulfate was applied to the coupon before any ash was applied to simulate the formation of an initial sodium sulfate layer.

During the experiments, simulated flue gas (Table 5) flowed through the reactor. During some of the experiments, a high purity alumina boat was placed in the furnace upstream of the samples. The boat contained 3 grams of Na₂SO₄ which allowed a constant atmosphere of sodium sulfate vapor to flow over the samples. At 1173 K, little of the sodium sulfate evaporated from the boat.

Table 5. Flue Gas Mixture

O ₂	5.0%
H ₂ O	5.6%
SO ₂	1917 ppm
N ₂	Balance

Coupons from three experimental conditions were analyzed at EERC. The samples were set in epoxy resin and cut in half. The samples were not polished and there appeared to be some oxidation of the silicon carbide surface because of this. The cross-sections were examined via SEM and EDX analysis. Elemental composition was determined at individual points or mapped over the entire image.

The first sample consisted of Wyodak ash only on a siliconized SiC substrate. The ash on the surface of the coupon did not appear to have melted, although considerable sintering of initial, fine ash to form larger ash particles was noted. The ash did not cover the surface completely. The elemental maps showed no clear reaction layer between the silicon carbide and the ash; the ash constituents (Al, Ca, Mg) were distributed uniformly throughout the ash. Some horizontal cracking in the SiC was seen within about 20 microns of the surface; this may be an artifact of the sample preparation technique.

The second sample consisted of a layer of sodium sulfate ash subsequently exposed to vapor-phase sodium sulfate with no coal ash. The initial layer of sodium sulfate was calculated to be 19 microns thick based on a loading of 5 mg/cm^2 . Visual observations revealed a layer of sodium sulfate approximately 20 microns thick on the surface. Some of the silicon in the substrate moved into the sodium sulfate layer in discrete domains. These regions were probably silica glass, possibly containing some sodium, although none was detected in the EDX maps, indicating that, if there were sodium, it was present in low concentrations. Thus there is some evidence of reaction between silicon carbide and sodium sulfate at 1173 K over 200 hours.

The final sample consisted of an initial layer of sodium sulfate topped with size segregated Wyodak ash. The coupon was exposed to vapor-phase sodium sulfate during the experiment. There appeared to be some surface pitting. No sodium sulfate layer was observed on the surface. In the absence of ash (Sample 2), the initial sodium sulfate layer was preserved on the surface. However, in Sample 3, the sodium sulfate reacted with the ash. EDX maps showed that sodium and sulfur are dispersed throughout the ash. Most of the silicon carbide surface was covered with a phase composed of CaO , MgO , and SiO_2 . The composition is not similar to the ash composition, thus this seems to be a reaction product of the ash and silicon carbide. Note that for Sample 1, which consisted of ash only, this reaction product was not observed. It appears that the presence of sodium sulfate encouraged a reaction between the ash and the silicon carbide.

In summary, ash alone showed little evidence of corrosion in the siliconized silicon carbide at 1173 K (1650F). Sodium sulfate by itself produced a silica-rich reaction product. By far the largest amount of corrosion was observed when sodium sulfate was combined with Wyodak ash. A reaction product (CaO-MgO-SiO_2) covered most of the surface; in this case the surface showed some evidence of pitting.

Task 3.2.3. Deposit Initiation and Growth on Ceramic Surfaces

Coatings and refractory ceramic materials were tested this quarter for resistance to slag corrosion. The tests are designed to indicate materials that could be used as or to protect slag dams in the slag corrosion sting tests of structural ceramics, as well as protect the ceramic air heater in the radiant zone of a high-temperature advanced furnace (HITAF). Emphasis is being placed on materials that satisfy both requirements, although materials that are effective in only one use are being studied. The coatings are primarily required to be very highly resistive to slag attack, field applicable to large structures, and compatible with both refractory and structural ceramic materials. The refractories should also be resistive to attack, field applicable, and compatible with structural ceramics, but also have relatively high thermal conductivities since they will be much thicker than the coatings. All tests were performed under the most severe conditions that will be used in the sting tests, i.e., using a high-calcium coal ash and a temperature of 1700 K (2600F).

Task 3.2.3.1. Refractory Tests

Several refractory boats were constructed (Figure 25) to test the corrosion resistance of refractories and a structural silicon carbide (a-sintered Hexolloy SA) in the presence of coal slag. The cup of each refractory boat was used to contain the coal slag during the slag tests.

Blocks of Hexolloy SA SiC were placed into four of the refractory boats, which included one SiC rammable, one SiC castable, and one alumina castable (Table 6). Small amounts

Figure 25. Cross Section Through Refractory Boat Design Used for Combustion 2000 Slag Tests

Figure 26. Scanning Electron Micrograph Showing Large, Iron-Rich Phase Located Micropit in Hexolloy Surface. Iron-Rich Grain is Suspended in the Slag.

Table 6. Technical Data for the Carborundum Refractories Used in Slag Test 1

Material Type:	SiC Castable	SiC Rammable	Alumina Castable
Maximum Temperature			
_C	1480	1600	1815
_F	2700	2900	3300
Composition, wt%			
SiC	83	89	
SiO ₂	.0	.3	
Al ₂ O ₃	1	6	96
CaO	.3	.0	.0
Fe ₂ O ₃	12	4	3
P ₂ O ₅	.5	.1	.6
	2		0
	.8	0	.1
	0	.3	
	.2	1	
		.7	
Thermal Conductivity			
W/m K @1477 K	5.8-8.7	5.8-8.7	1.7-2.0
Btu in/hr ft ² _F @2200_F	40-60	40-60	12-14
Density			
g/cm ³	2.32	2.53	2.72
lb/ft ³	145	158	170

of refractory were rammed into the openings between the ceramic block and the refractory to prevent slag infiltration. This additional refractory material was not cured or dried due to time constraints. Approximately 2.5 grams of a basic ash (base/acid = 1.12) was poured into the cup of each boat and directly onto the surface of the SiC ceramic blocks.

The boats were raised to a temperature of 1700 K (2600F) at a rate of 144 K per hour and held for 155 hours. After the 155-hour exposure, the materials were brought to room temperature at a rate of 144 K per hour, cross-sectioned, and examined using scanning electron microscopy (SEM) with energy dispersive x-ray analysis. Throughout the text, these samples will be referred to as samples 1 through 3.

The refractories were mixed and prefired according to manufacturer's instructions. The prefired SiC refractories were very porous and vesicular and seemed to have reacted with the furnace substrate. The alumina refractory did not show any physical degradation due to prefiring.

Physical Description of Sample 1

On the top surface of the castable SiC refractory (Sample 1) were large, irregular, iridescent SiC grains approximately 10 to 30 μm in length. These grains were surrounded by cavities 1 to 3 mm in diameter and were covered by clear and white glass. The presence of frothy and vesicular glass indicated gas evolution during the exposure. A few occurrences of a blue-green transparent phase and some smaller, white, isometric crystals were also present in the glass on the refractory surface. The sides of the refractory were similar in appearance to the top surface. Extensive gas evolution had occurred, large surface pits were present, and clear and white glasses and white crystals were present. The base of the refractory had SiC grains embedded in a milky white glass, but the glass was brownish and iridescent near the edges of the block.

In cross section, the Hexolloy had an undulating top surface which was overlaid by white glass. The slag seemed to be well bonded to the Hexolloy. The white glass formed a fairly continuous layer approximately 200 μm thick and was overlaid by a greenish-brown glass. A highly reflective, reddish brown, opaque phase was present within some of the micropits, and vesicles were present in close proximity to these micropits. In a few of these areas, this reflective phase, which was later identified as an iron-rich phase, formed underneath the white glassy layer and next to the exposed Hexolloy surface. The iron-rich grains ranged in size from a few microns to 500 μm in diameter and varied in shape, although many of the grains had a very circular cross section. Most of the grains were in the range of 10 μm in diameter.

The depth of pitting of the Hexolloy surface in Sample 1 reached a maximum of 300 μm in some areas after the 155 hours of exposure. Many of the larger pits contained the iron-rich phases, and the proximity of these phases to the pits indicates that they were reaction products from the corrosion of the SiC by slag constituents. Other studies have identified iron silicides as reaction products in the corrosion of silicon carbides by coal slags (Ferber and Tennery, 1982).

Chemical Description of Sample 1

The coal ash was analyzed by x-ray fluorescence. The average composition of the slag remaining in Sample 1 shows that the concentrations of MgO, SO₃, CaO, and Fe₂O₃ all decreased from the ash to the amorphous slag composition, but CaO showed the most

substantial decrease from 34% in the ash to less than 10% (wt) in the slag. This dramatic decrease in CaO content is difficult to explain. Analyses of glass on the sides of the refractory material indicated higher amounts of CaO than the vendor's analysis; therefore, CaO from the slag may have been incorporated into the refractory glass. Because SO_3 is volatile, its concentration would be expected to decrease from the ash to the slag analysis. The Fe_2O_3 from the slag was probably incorporated into the iron-rich phases located along the Hexolloy surface. These iron-rich phases are assumed to be low melting temperature iron silicides that have been described in similar experiments (Ferber and Tennery, 1982). Iron silicides were found to be the reaction products of the dissolution of SiC by basic coal slags and have a melting temperature below 1400C.

Silica was the only major constituent within the slag of Sample 1 that showed an increase from the ash analysis from 33% to 75%. This increase in SiO_2 can be explained by the dissolution of SiC grains from the Hexolloy or the refractory into the slag. Several grains of SiC, distinguished by low oxygen counts, were detected within the slag.

Other phases present in the slag included SiC grains that were dissolved either from the refractory or the Hexolloy surface during exposure and iron-containing phases concentrated along the slag/Hexolloy interface and around SiC grains (Figure 26). The iron-containing phases were similar in composition to iron silicides and iron phosphides (Fe_2Si and FeP_2). The presence of these phases indicates that highly reducing conditions were present locally within the slag. Some of the larger iron-rich grains varied in composition within a single grain or cluster of grains. The source of the phosphorous may have been the slag, although the coal ash contained only one percent P_2O_5 , or the refractory material, which may contain phosphorus in the binder. The phosphorus may have aided the corrosion of the SiC by producing areas of lower viscosity, therefore, aiding the diffusion process of corrosive elements to the Hexolloy surface. Auger analysis of an iron-rich grain showed no oxygen or carbon present in the interior.

Silicon was concentrated at the surface of the particle. Phosphorus seems to be concentrated in a ring around the center of the iron-rich particle. Iron concentrations are highest at the top edge and at the center. We believe some phase separation occurred within the particle during cooling, without crystallization, to give the complex, layered composition of the iron-rich particle. The presence of reduced phases indicates the iron-silicide-phosphide particles form under oxygen-poor conditions adjacent to the silicon carbide.

The sides of the refractory were also examined by SEM to determine the cause of porosity and glass formation during prefiring and during the slag exposure for Sample 1. The surface was uneven, pitted, and contained frothy areas indicative of gas evolution. The SiC grains were coated with a clear, calcium aluminosilicate glass with a fairly homogeneous composition. No other reaction products were found. The calcium and alumina in the glass could have originated from the refractory material, the coal slag, or the furnace substrate, which adhered to the sample. According to the manufacturer's analyses, the refractory material contained 83% SiC, 3% CaO, 12% Al_2O_3 , and 1% SiO_2 and could have been a source for the glass formation. It is also possible that the original refractory materials were contaminated.

Physical Description of Sample 2

The white slag glass overlying the Hexolloy SA contained incorporated refractory grains. Some areas of the glass were clear and contained small, white, isometric crystals. The

SiC grains on the side of the refractory were coated with a milky white glass within a clear glass matrix. Numerous vesicles, 3 to 10 μm in diameter, indicated large amounts of gas evolution. Very large vesicles (35 to 60 μm) were present in the slag above the Hexolloy surface. The Hexolloy surface was undulating and well bonded to the overlying greyish-white glass. Iron-rich phases were present within the slag and at the Hexolloy/slag interface and ranged in size from 5 to 100 μm in diameter. The milky-white glass above the SiC ceramic contained very large gas bubbles and white, isometric crystals. Some SiC grains were incorporated into the glass and contained reddish reaction rims.

A few areas along the Hexolloy/slag interface showed little or no interaction. The localized pitting of the surface may have been related to compositional variations within the slag during the exposure. Some fluxing agents within the slag, such as calcium, phosphorus, or iron, could have locally reduced the slag viscosity and facilitated diffusion of corrosive species to the Hexolloy surface.

Chemical Description of Sample 2

The slag composition from Sample 2, composed of the rammable SiC refractory and Hexolloy SA SiC, was similar to Sample 1, although the alumina and P_2O_5 contents showed minor increases. The rammable refractory is a phosphate-bonded material, which would explain the increase in P_2O_5 content. As in Sample 1, there was a dramatic decrease in CaO concentration, but some of the CaO may have been incorporated into the glass coating on the refractory.

The slag had a fairly uniform calcium aluminosilicate composition, although a few points contained higher amounts of calcium and alumina. The iron-rich phases were possibly iron silicides and iron phosphides. The compositions of these phases were variable within large grains, which may have been composed of several smaller grains. Several grains of SiC within the slag were coated with similar, iron-rich phases. This indicates that the iron-rich phases were reaction products formed from the SiC.

Physical Description of Sample 3

The alumina refractory containing the Hexolloy SA SiC (Sample 3) reacted quite differently than the SiC refractories. Only a very thin layer (<70 μm) of slag remained on the Hexolloy surface after the exposure. The top surface of the Hexolloy was undulating in cross section, but was not well bonded to the overlying slag layer. The slag was orange in appearance, but did not seem to contain any recognizable phases. This thin layer of yellowish glass contained extensive cracks that were probably formed during cooling due to differences in thermal expansions of the Hexolloy and the slag. Most of the slag was absorbed quite readily into the alumina refractory, as evidenced by the yellow discoloration of the material in contact with the slag.

The top of the refractory was very granular and porous in appearance. The original alumina grains varied in size and were angular and irregularly shaped. Although slag infiltration caused discoloration in the surrounding matrix, the large alumina refractory grains did not seem to be affected by the slag. The sides of the refractory were fairly dense and contained a glassy or microcrystalline matrix with infrequent, small micropits.

Chemical Description of Sample 3

The absorption of the slag into Sample 3 would explain the differences between the original ash and the final slag compositions. The alumina and silica contents of the slag were higher than the ash contents, while the calcium and iron contents were lower. Some analyses within the refractory indicated high amounts of calcium. The residual slag above the Hexolloy contained two primary phases: elongated SiC grains and an interstitial high-silicon-iron glass. Several grains of iron phosphide and iron silicide were present along the top surface of the Hexolloy.

Little interaction occurred between the refractory and the Hexolloy, and the refractory was not bonded to the Hexolloy after the exposure. Near the slag/refractory interface, the refractory varied in composition. Most of the analyses indicated a high alumina material, but some areas contained a sodium aluminosilicate phase. This phase could not be distinguished optically from the surrounding material.

Summary

In general, the high porosity and reactivity of the SiC and alumina refractories made them inappropriate in the sting tests. The high porosity allowed extensive slag infiltration and reaction causing the composition of the slag to change dramatically. The refractories all contain a high temperature binder designed to bond grains (of SiC or alumina) together. Aluminosilicate glasses containing calcium or phosphorous were used as binders in the refractories tested. Reaction of the binders with slag was evident.

Next quarter several approaches will be pursued to improve resistance of refractories to high-calcium slag including coatings and new binder formulations. Testing of a bituminous slag which should be less reactive will also commence next quarter.

3.2.3.2. Coating Tests

Materials were tested this quarter for their suitability as coatings to reduce slag attack of HITAF refractory and structural ceramic materials. They included paintable stabilized zirconium oxide, paintable alumina/sodium silicate, two paintable mixtures of calcia and fly ash, and one paintable mixture of magnesia and fly ash. The paints were tested for their ability to bond to alumina refractory in order to prevent its corrosion in high-calcium slags. Alumina and mullite refractories are being used in coating tests because even though they are chemically attacked by high-calcium slag, they otherwise have good physical properties and also contain little binder material that may destroy the coating. Stabilized zirconium oxide paint was tested because of its known resistance to chemical corrosion and because of its commercial availability. The alumina/sodium silicate paint was tested because of its hardness, bonding properties, and commercial availability, although its corrosion resistance may be limited. In addition, two mixtures of calcia and fly ash and one mixture of magnesia and fly ash were tested as corrosion-inhibiting coatings for refractory or structural ceramics. Such mixtures could be used as slag-dam coatings in the sting tests since even if they partially dissolve in the slag, they would not substantially change its composition. Also, they could serve as coatings of the HITAF radiant air heater that could be renewed during normal operation through the addition of calcia or magnesia to the coal feed.

The stability tests of the refractories have shown that none are suitable for a high-calcium slag at 1700 K (2600F). The alumina-based materials failed for reasons of chemical reactivity, while the SiC-based materials failed for both chemical and physical reasons, primarily macroporosity. Therefore, tests were initiated to find coatings to provide some

chemical corrosion protection to the refractories that are physically viable, or even to the structural silicon carbide. The tests were designed to provide information about coatings that could be used on the refractory slag dams for the sting tests and also for use in the HITAF system. The criteria for final selection are high-temperature stability, abrasion resistance, chemical stability toward the slag and the refractory, and the ability to be renewed in the field. Two research directions were taken: the use of commercially available paintable coatings that could be applied in a HITAF system when off-line, and the use of a coal additive that would protect the refractory or air heater by forming a nonreactive high-viscosity layer at the slag interface. The advantage with using coal additives is that the coatings could be renewed on-line, most likely by introducing them into the HITAF in pulses with the coal feed.

The two commercially available paintable products included a 97% calcium-stabilized zirconium oxide with 3% alumina binder and an alumina and sodium silicate blend that is used as a cement for alumina products. Each was painted on both air-dried and fired castable alumina refractory and fired to 1700 K (2600F). Both coatings bonded equally well to the air-dried and prefired refractory. The zirconia was approximately as hard as the refractory, though neither are very resistant to abrasion. The alumina/sodium silicate ratio is proprietary, so it is not known how reactive the material will be to coal slags, although it is not expected to vitrify below 1920 K (3000F). The slag-corrosion resistance of the materials will be tested in the next quarter.

In addition to the commercially available coatings, calcia and magnesia were tested for their ability to mix with PRB coal ash to make it less reactive toward alumina refractory. Normally, the calcium in the slag reacts with the alumina to form calcium hexaluminate which spalls and does not form a protective coating on the alumina (Bonar and others, 1979). The reaction with PRB coal slag is especially rapid because of the low viscosity of the slag. To make the slag less reactive, one can use additives to increase its viscosity or even solidify the slag, although calculating the exact effects of an additive are often difficult due to the complex composition of the slag. For the tests performed this quarter, it was assumed that the $\text{MgO-CaO-Al}_2\text{O}_3\text{-SiO}_2$ system would approximate the melting behavior of the ash since those oxides make up 90% of the total and all others individually make up less than 5% of the ash. All calculations were based on phase diagrams given by Levin and others (1969) and Levin and McMurdie (1975). Within the $\text{MgO-CaO-Al}_2\text{O}_3\text{-SiO}_2$ system, the composition of the ash places it in a melilite phase field with a liquidus temperature of 1505 K (2550F) and a solidus temperature below 1589 K (2400F). Three attempts were made to shift composition of the ash to a different phase field with higher liquidus and solidus temperatures. For the first two attempts, calcium hydroxide was added to increase the CaO content of the ash to 1.5 and 2 times the original CaO level. Normally, the addition of calcia to a slag that is to be held in an alumina-based boat would be contraindicated because of the possible formation of calcium hexaluminate which would cause corrosion of the boat. The additions were made, however, to force the composition to such high calcia contents that the ash would remain solid at the slag test experiment temperature and thus greatly reduce the reaction rate. The 1.5 times calcia mixture was formulated to shift the composition to the dicalcium silicate (Ca_2SiO_4) phase field with a liquidus of 2089 K (3300_F) and a solidus of 1600 K (2420F). The 2 times calcia mixture shifted the ash composition to the lime (CaO) phase field with a liquidus of 2478 K (4000F) and a solidus of 1672 K (2550F). In addition, a 2.5 times magnesia mixture was prepared to shift the composition to the (periclase) phase field with a liquidus temperature of 2033 K (3200F) and a solidus temperature of 1672 K (2550F). Water was added to the ash and additive mixtures, and they were painted on both prefired and air-dried alumina refractory coupons. The ash/additive/water mixtures flowed and wetted the refractory well, although they

exhibited dilatant behavior (meaning they flowed under low stress but were rather stiff under higher stresses), which is normally a negative attribute for a paint. They would also make good aqueous slips, since the water was easily absorbed into the refractory, leaving a solid coating of the ash/additive mixture.

After the strips of coating material were painted on, the alumina refractory were heated in stages to 1744 K (2700F) and held for six hours by following the firing routine of some mullite slag cups that were also in the furnace. The coupons were oriented vertically to allow the ash/additive mixtures to flow if they melted. After firing, the coupons were quenched. Little difference was seen between the coatings on the air-dried versus the prefired coupons. None of the coatings melted and flowed down the face of the coupon, although they all were relatively well sintered. This is in marked contrast to the ash which forms a flowing slag at this temperature.

Both of the calcia/ash mixtures had dissolved into the alumina, leaving an inner sintered layer and an outer glassy layer. The reaction products were raised above the surrounding coupons. The interaction indicates that the calcium in the mixture reacted with the alumina before it fused with the ash. Although this problem can be circumvented by fusing and grinding the mixture before the paint is made, it does indicate that the use of calcia as an additive is contraindicated in a system employing alumina refractory if any of the refractory is not covered with slag before the addition of the calcia. Also, the concentration of calcia should not be increased to shift the composition to the lime phase field, or the lime that precipitates from the mixture may strongly interact with the refractory.

In contrast to the calcia/ash mixture, the magnesia/ash mixture was well sintered, although vesicles were evident at the surface. The material was bonded to the alumina, although cracking occurred at the coating/alumina interface, possibly because of mismatches in coefficients of thermal expansion. Only slight chemical interaction with the refractory was evident. This implies that the magnesia may make a viable additive to solidify the slag next to the HITAF walls to reduce corrosion rates, although much more testing is recommended. The reaction products from both the calcia and magnesia tests will be analyzed in the next quarter by scanning or Auger electron microscopy to determine the mechanism of interaction with the alumina and also determine a more efficacious mixture composition.

TASK 3.3.1 - HITAF COMBUSTOR MODELING

Task 3.3.1.1 - Submodel Improvement

The ability of the combustion modeling tools to predict NO_x formation is important in the design stages of the HITAF. In order to insure that the combustion model contains the best science possible for describing the physical phenomena of the conversion of fuel-bound and molecular nitrogen to nitrogen oxides, a nitrogen submodel team was organized to review the current state of knowledge of fuel-nitrogen evolution and conversion as compared to the current nitrogen submodel in the comprehensive combustion model. Various experts in the areas of fuel nitrogen devolatilization, char nitrogen conversion, and homogeneous gas-phase nitrogen conversion have met and are developing the science necessary to provide a more physically descriptive nitrogen submodel for use with the combustion simulations. In addition, extensive revisions to the current comprehensive model are required and are currently underway to allow the tracking of additional information over the entire computational domain. The additional

computational information will allow implementation of the more complicated submodel for fuel nitrogen evolution.

The attainment of the design heat exchanger outlet temperature has been determined to be one of the primary design parameters affecting the overall size of the combustor; therefore, a parametric study was performed to define the geometric limits of the HITAF which would satisfy a number of operational constraints. Studies performed by the ash management group indicated that ash deposits would be difficult to clean from the interior walls of the HITAF at lower surface temperatures due to fusion of the ash layer; therefore, the decision was made to operate the HITAF with higher wall temperatures to maintain wet walls for ash manageability. The design constraints for the parametric study consisted, therefore, of achieving the required heat exchanger outlet temperature while maintaining wall temperatures that will promote flowing slag and minimizing the overall flue gas exit temperature. A variety of design parameters were investigated as to their impact on the ability of the HITAF to operate within these constraints. It should also be noted that the parametric computations represented a single silo of a dual silo HITAF, where the combined thermal output of the two silos would yield the design output of 268 MW_e.

A number of geometries, including both diameter and length variations, were investigated to define the size constraints for the HITAF. Both the premixed type and the long, axial flame type combustor were evaluated indicating the bounds on different heat release rates from the coal. The premixed simulation represents the most rapid rate of heat release, and the long flame represents the most delayed heat release. For an inner chamber diameter of 8 m, Figure 27 illustrates the window of combustor lengths over which the combustion model predicts that the heat exchanger fluid will achieve the design outlet temperature. The region exceeding 1255 K between the results for the premixed simulation and the long flame simulation indicates the region of operation.

Additional computations were performed utilizing several diameters and combustor lengths for the pre-mixed type combustor and the impact of these geometric variations on the heat exchanger outlet temperature are shown in Figure 28. The lengths required to achieve the design temperature for the different diameters is clearly evident in the figure. Other information available from these same computations included the wall surface temperature and the flue gas exit temperature for each of the different geometries. As there were additional design constraints involving these two temperatures as well, the impact of the geometrical variations on all three temperatures, the heat exchanger outlet temperature, the furnace wall surface temperature, and the flue gas exit temperature, were superimposed to obtain overlapping regions of HITAF operation. The effects of the size variations on the different temperatures had conflicting impacts. Increases in combustor size caused favorable increases in heat exchanger outlet temperatures and favorable decreases in the flue gas exit temperature. The furnace wall temperature, however, shows an unfavorable decrease with increases in combustor size which can lead to conditions where the slag is no longer flowing on the HITAF walls. An examination of the superimposed areas of operation for the three temperature limits indicated an optimal size which minimized flue gas exit temperature while still achieving the heat exchanger outlet temperature and maintaining flowing walls. A premixed combustor of approximately 9 m diameter by 25 m in length appeared to be the optimum geometry with a slightly longer combustor required for the long axial variation (cf. Figure 27).

The effect of various wall resistances was investigated as well, in close cooperation with the ash management group, and the result of the various calculations indicated that the

HITAF is capable of transferring the appropriate amount of heat to the heat exchanger fluid with reasonably dirty walls. Under highly fouled conditions, however, it will become increasingly more difficult to achieve temperature and may in fact require additional heat exchanger area (*e.g.*, length) to provide a margin of safety.

Figure 27. Effect of length and heat release rate on heat exchanger air outlet temperature.

Figure 28. Effect of combustor geometry on the heat exchanger air outlet temperature for the premixed combustor.

Variations in the temperature of the radiant heat exchanger inlet air indicated an essentially linear increase of the outlet temperature with increases in inlet air temperature and provided an impetus to attempting additional heat transfer in the convective heat exchanger prior to entering the radiant heat exchanger. Any additional enthalpy in the heat exchanger inlet air would provide a greater margin of safety in terms of the degree of fouling that can be allowed on the radiant panel surfaces.

An additional design variation that was examined was the use of either co-current flow or counter-current flow in the radiant heat exchanger. As a result of the high temperature driving force between the combustion gases and the heat exchanger air, there did not appear to be much difference between the two cases. It was possible to achieve the design heat exchanger outlet temperature with either configuration. It is important, however, to maintain flowing walls in the radiant section of the HITAF; therefore, a co-current heat exchanger would provide higher wall temperatures at the furnace outlet and would facilitate the maintenance of flowing walls. In addition, a co-current heat exchanger would result in the high-temperature outlet being nearer ground level, which may be more advantageous with respect to plumbing and insulation.

The previous calculations all utilized a Utah bituminous coal; therefore, the impact of different coal types was determined by comparing the results from a calculation using a Powder River Basin coal (Wyodak) with the same geometrical inputs as with the Utah bituminous coal. The thermal profiles for the two cases are shown in Figures 29 and 30. Figure 29 illustrates the gas centerline, furnace wall and heat exchanger air temperature profiles for the Utah bituminous coal, and Figure 30 shows the same temperature profiles for the Powder River Basin coal. As is shown, the Powder River Basin coal burns slightly hotter and results in somewhat higher gas and furnace wall temperatures. In both cases, however, the design heat exchanger outlet temperature was achieved.

Figure 29. Temperature profiles for a Utah Bituminous coal with a combustor geometry of 8 m diameter and 20 m length.

Figure 30. Temperature profiles for a Powder River Basin coal (Wyodak) with a combustor geometry of 8 m diameter and 20 m length.

Task 3.3.4. Mineral Matter Transformations

Modifications were made to the mineral matter transformation model to allow it to interface with the ash transport and deposition model. The ash transport and deposition model was completed. This model calculates the ash loading in the gas and the ash deposition rate to the walls from the furnace hot zone to the convective air heater inlet.

Task 3.3.5. Model Slag Behavior

This quarter, the HITAF ash deposition and transport model was completed. The purpose of this model is to predict ash deposition, ash particle loading, and composition of entrained ash and deposits in the HITAF. The model will be described first and the results for Illinois 6 (raw coal as specified by EPRI TAG) will be discussed.

Information on the coal mineral composition and size distribution obtained from CCSEM analysis is used to predict the ash size and composition distribution. PSIT's Mineral Matter Transformation model is utilized to make the calculation. The combustion stoichiometry is taken from the current combustor design: 740 MBtu/hr at a stoichiometric ratio of 1.0. Secondary combustion air is assumed to be vitiated to 17.5% oxygen. Transport air, which accounts for 15% of the oxygen, is assumed to have 21% oxygen. The adiabatic flame temperature is calculated, together with the amount of heat needed to raise the working fluid in the radiant air heater from 1300F to 1800F. The flue gas exit temperature is calculated from the difference between these two quantities.

Ash deposition on the radiant air heater walls is calculated based on the turbulent deposition flux discussed in Quarterly Report 1. After dilution, gas enters the slag screen. Design of the slag screen was discussed in Quarterly Report 1. Heat transfer to the tubes is calculated, giving a temperature drop across the slag screen. The sticking coefficient for ash particles in the slag screen is assumed to be unity.

After the slag screen, the gas encounters the convective air heater. This is assumed to be made of finned tube sheets. Deposition on the leading edge of the convective air heater is modeled as impaction on a cylinder in cross-flow. Again, the sticking coefficient is assumed to be unity. Deposition within the convective air heater is modeled using the turbulent deposition flux above.

The results of these calculations are shown in Figure 31 and Table 7. The figure shows the mass loading of ash in the gas at various points in the system. There is little change in the mass loading at the exit of the radiant heater; the turbulent diffusion flux is quite low. After the slag screen, particles larger than 20 microns are removed from the gas

Figure 31. Ash Particle Size Distributions - Illinois 6 (Raw)

Table 7. Deposit Composition (in mole %)

Oxide	Initial Ash	Radiant AH	Slag Screen	Leading Edge
SiO ₂	61 .2	61 .1	59 .5	63 .2
TiO ₂	1 .2	1 .2	1 .2	1 .3
Al ₂ O ₃	13 .5	13 .5	13 .2	14 .2
Fe ₂ O ₃	11 .4	11 .6	12 .6	10 .3
CaO	10 .1	10 .3	11 .2	8 .9
MgO	0 .3	0 .3	0 .4	0 .3
Na ₂ O	0 .6	0 .6	0 .6	0 .6
K ₂ O	1 .4	1 .4	1 .4	1 .4

and particles in the range of 10 to 20 microns are significantly reduced. The leading edge of the convective air heater is predicted to remove a small amount of the 10 to 20 micron particles. Most of the particle removal takes place in the slag screen which is assumed to be very efficient, more efficient than the screen tubes currently in use in wet-bottom boilers. If the slag screen were not so efficient, the leading edge of the convective air heater would see a much higher particle deposition rate.

Table 7 shows the deposit composition at various points in the HITAF. For the Illinois 6 coal, the composition of the deposits is close to the bulk ash distribution. The slag screen deposits are slightly enriched in iron because of the large pyrite-derived particles that are removed preferentially.

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