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**THE VENTING CHARACTERISTICS OF A FIBROUS
THERMAL BARRIER SYSTEM DURING A RAPID
DEPRESSURIZATION ACCIDENT (ROTOR FAILURE)
IN AN HTGR-GT**

by

S. LIGHTNER and W. S. BETTS, JR.

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GENERAL ATOMIC COMPANY

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THE VENTING CHARACTERISTICS OF A FIBROUS THERMAL BARRIER SYSTEM
DURING A RAPID DEPRESSURIZATION ACCIDENT
(ROTOR FAILURE) IN AN HTGR-GT

S. Lightner
General Atomic Company
San Diego, Calif.
Assoc. Mem. ASME

W. S. Betts, Jr.
General Atomic Company
San Diego, Calif.

The thermal barrier inside the primary circuit of the direct-cycle, high-temperature gas-cooled reactor-gas turbine (HTGR-GT) may experience rapid depressurization transients. Turbine rotor failure induces the most severe pressure transient. To determine how rotor failure affects the design adequacy of the thermal barrier, the authors experimentally examined the venting characteristics of the fibrous insulation. This paper theoretically develops a simulation technique and correlates the analytical results with test data.

NOMENCLATURE

C' Non-Darcian permeability coefficient, s
K Darcian permeability coefficient, m^2
P gas pressure, MPa
P_i pressure at location number i, where
i = 1, 2, ..., 9, Pa
P₀ initial pressure at start of
depressurization
 ΔP_{MAX} (P₁ - P₄)_{MAX}, maximum pressure drop across
fibrous insulation and perforated tube, MPa
 ΔP_{∞} (P₁ - P₄) _{∞} , pressure drop across fibrous
insulation and perforated tube when the
pressure should be at equilibrium, MPa
 ΔP_{SS} pressure drop across seal sheet of thermal
barrier, MPa
 ΔP_{CP} pressure drop across coverplate of thermal
barrier, MPa
 ΔP_{FI} pressure drop across fibrous insulation of
thermal barrier, MPa
R gas constant, J/(kg·K)
t time, hr
T average temperature of the fibrous
insulation, K
u velocity of flow through porous medium, m/s
x distance, m
 ϵ porosity of porous medium

μ viscosity of gas, N·s/m²
 ρ density, kg/m³
 ∇ the vector (or del) operator = $i(\partial/\partial x) + j(\partial/\partial y) + k(\partial/\partial z)$ in cartesian coordinates
 ∇^2 the Laplacian = $(\partial^2/\partial x^2) + (\partial^2/\partial y^2) + (\partial^2/\partial z^2)$ in cartesian coordinates

INTRODUCTION

The thermal barrier of the high-temperature gas-cooled reactor (HTGR) protects the liner of the prestressed concrete reactor vessel (PCRV) and the adjacent concrete from the high-temperature helium of the primary coolant system. This thermal barrier generally consists of fibrous insulation which is retained against the liner surface by metallic coverplates and attachment fixtures. Figure 1 shows a typical thermal barrier. The assembly includes seal sheets, located between the fibrous insulation and the metallic coverplates. These seal sheets restrict gas permeation during normal operation and contain the insulating ceramic fiber of the blankets.

This thermal barrier system must withstand environmental conditions such as pressure transients, temperature transients, acoustic excitation, flow-induced vibration, irradiation, and compatibility with reactor helium. In the direct-cycle HTGR-gas turbine (GT) plant, the pressure transients due to turbine deblading or rotor failures may be significantly more severe than those encountered in other types of HTGRs. Studies indicate that these pressure transients may induce loads which could control the design of the thermal barrier. This paper discusses the results of experimental and analytical studies to develop techniques to predict the loading on a fibrous thermal barrier system.

DISCUSSION OF PROBLEM

Due to the rapidity with which the reactor may

depressurize during a rotor failure accident, the pressure response of the gas within the thermal barrier will lag the pressure changes of the primary coolant. This lag causes a pressure differential to occur across the thermal barrier. This pressure differential acts as if it were a uniform pressure load. The stresses induced by this effective pressure load in the thermal barrier coverplate and attachment fixture must be evaluated to assure design adequacy. Figure 1 indicates the components of this total pressure load (ΔP_T), and the following equation relates them to the effective, uniform pressure load:

$$\Delta P_T = \Delta P_{CP} + \Delta P_{SS} + \Delta P_{FI} \quad (1)$$

The flow resistance of the fibers determines the rate of insulation depressurization and, consequently, the pressure loads which will be experienced by the thermal barrier coverplate. Therefore, a simulation technique for the pressure response of the fibrous insulation needed to be verified. The results of thermal barrier rapid depressurization tests conducted at Eidgenössisches Institut für Reaktorforschung (EIR), Würenlingen, Switzerland, provided an opportunity to examine the simulation technique suggested in Ref. 1.

Fig. 1 Typical thermal barrier configuration for hot regions of the HTGR-GT

TEST DATA INFORMATION

EIR conducted rapid depressurization tests for a fibrous thermal insulation system. Figure 2 shows the test arrangement. This model was filled with compressed air, then rapidly depressurized. Figure 2 also shows the locations of the piezo-electric pressure transducers which recorded the local pressure transients. These pressure gages were placed at three different axial locations. For each axial position, three pressure transducers were located as follows: within the fibrous insulation, inside the vent cavity, and on the inner surface of the coverplate. The pressure response of these gages was recorded as a function of time.

Experiments were conducted at three different initial pressure levels, which varied from ~1 to 4 MPa, and for depressurization rates from 1.2 to 400 MPa/s. Importantly, the EIR tests concluded that the axial pressure differences in the test rig were negligible. This situation made one-dimensional simulation of the test conditions reasonable. After completion of the experiments, the test rig was

ALL DIMENSIONS ARE IN mm UNLESS OTHERWISE SPECIFIED

Fig. 2 Test rig and locations of pressure gages 1 through 9

taken apart, and the fibrous insulation was examined. Despite the large pressure drops across the insulation during this test, no insulation damage was detected. No displacement of the insulation and no dust from the fibrous insulation in the test assembly were detected. Compression of the fiber blankets may have occurred during the test; however, this information was not available due to the test rig limitations.

ANALYSIS

Reference 1 developed a method to computer-simulate pressure transients through a porous medium. This method is based upon the following assumptions: (1) Darcy's law is applicable; (2) the ideal gas law is valid; and (3) low-velocity flow exists through the porous medium. Using the approach of Ref. 1 and beginning with the continuity equation,

$$\nabla \cdot (\rho \vec{u}) + \epsilon \frac{\partial \rho}{\partial t} = 0 \quad (2)$$

Reference 1 used Darcy's Law to find $\vec{u}$:

$$\vec{u} = -\frac{K}{\mu} \nabla P \quad (3)$$

Then Ref. 1 substituted Eq. 3 into Eq. 2 to derive a new form of the continuity equation.

An empirical relation for μ was derived from

the EIR test results. This empirical relationship is given in Eq. 4 below:

$$\dot{u} = -\frac{C'}{\rho} \nabla P \quad (4)$$

Substituting Eq. 4 into Eq. 2 results in Eq. 5:

$$\nabla \cdot (-C' \nabla P) + \epsilon \frac{\partial \rho}{\partial t} = 0 \quad (5)$$

Using the ideal gas law, assuming T to be independent of time (i.e., isothermal expansion), and substituting into Eq. 5,

$$\nabla \cdot (-C' \nabla P) + \frac{\epsilon}{RT} \frac{\partial P}{\partial t} = 0 \quad (6)$$

Assuming that the porous medium is isotropic means that C' is independent of direction, and Eq. 6 becomes

$$\nabla^2 P = \frac{\epsilon}{C'RT} \frac{\partial P}{\partial t} \quad (7)$$

Since the EIR test results indicated that pressure differences in the axial direction were negligible, Eq. 7 can be rewritten in the one-dimensional form for use in a computer simulation of isothermal cases:

$$\frac{\partial^2 P}{\partial x^2} = \frac{\epsilon}{C'RT} \frac{\partial P}{\partial t} \quad (8)$$

Using the relationship between isothermal and adiabatic flow developed in Ref. 1, Eq. 9 is obtained for adiabatic cases:

$$\frac{\partial^2 P}{\partial x^2} = \frac{\epsilon}{\gamma C'RT} \frac{\partial P}{\partial t} \quad (9)$$

where γ = specific heat ratio (1.4 for air).

These equations were solved by using the TAC2D computer code (2). Table 1 gives transients that are representative of conditions in the EIR tests and were simulated using the one-dimensional TAC2D model of a radial section of the fibrous insulation in the test rig at the axial location of gages 4 and 1. These pressure boundary conditions were impressed upon the inner radial surface of the model; the other surfaces of the model were assumed to be impermeable. The properties used were

$$\begin{aligned} C' &= 5.86 \times 10^{-7} \text{ s,} \\ \epsilon &= 0.90 \text{ (for 300 kg/m}^3 \text{ Kaowool),} \\ T &= 294 \text{ K,} \\ R &= 287 \text{ J/(kg K).} \end{aligned}$$

This gives

$$\frac{\epsilon}{C'RT} = 18.2 \frac{\text{s}}{\text{m}^2} \quad (10)$$

CORRELATION BETWEEN ANALYTICAL AND TEST RESULTS

Figure 3 shows typical analytical results for the TAC2D simulation of the one-dimensional pressure response of the fibrous insulation. These results are based upon a non-Darcian permeation coefficient (C') of 5.86×10^{-7} s. This effective permeation

TABLE 1
PRESSURE AT GAGE 4 AS USED FOR PRESSURE
BOUNDARY OF THE TAC2D MODEL

Time (ms)	Pressure at P4 (MPa) (a)						
	HV6	HV8	HV10	HV12	HV13	HV14	HV15
0.	1.07	1.08	2.05	2.06	4.00	4.00	4.00
3.75	--	--	--	--	--	--	4.00
5.	--	--	--	1.67	--	--	3.60
10.	--	0.80	--	--	--	--	3.20
12.5	--	--	1.80	1.47	--	--	--
17.5	--	--	--	--	--	3.00	--
20.	--	--	--	--	--	--	2.65
25.	0.80	--	--	--	--	--	--
30.	--	--	--	--	2.99	--	2.40
40.	--	--	--	--	--	--	2.20
50.	0.67	0.60	1.20	1.10	2.70	2.25	2.00
100.	0.48	0.45	0.85	0.80	1.90	1.60	1.50
150.	--	--	0.60	--	--	--	--
200.	0.30	0.27	0.46	0.44	1.15	0.85	0.75
300.	0.20	0.17	0.24	0.23	0.51	0.50	0.40
400.	0.17	0.15	0.14	0.15	0.25	0.25	0.15
500.	0.17	0.15	0.10	0.12	0.15	0.15	0.10
600.	0.17	0.15	0.10	0.12	0.	--	0.10
700.	0.17	0.15	0.10	0.10	0.	--	0.10
750.	0.17	--	--	--	--	--	--
800.	--	0.15	--	0.10	--	0.15	--
850.	--	--	--	0.10	--	--	--

(a) The pressure profile is linear between the given data points. HVI is the case number of the individual EIR tests. The pressure profiles were intended to simulate the actual pressure experienced during the indicated case.

Fig. 3 Transient pressure profiles for the simulation simulation of case HV15 using isothermal flow conditions with the non-Darcian formulation

coefficient was derived for the fibrous insulation at 300 kg/m³ density in conjunction with the mesh grid and the perforated tube, as shown in Fig. 2. Since hand calculations showed that the flow resistance introduced in the combined system by the perforated tube was negligible and assumed true for the mesh, the non-Darcian permeation coefficient was assumed to be representative of the behavior of the fibrous insulation.

The Fig. 3 results indicate a continuously smooth pressure distribution through the thickness of the fibrous insulation, as expected. Node 11 is the approximate radial location of gage 1. Table 2 gives the maximum pressure drop from the inner radial boundary to node 11 in the fibrous insulation and the time of occurrence recorded for each transient. As Table 2 shows, the measured ΔP_{MAX} and the isothermal analytical results using the non-Darcian formulation agree well. Both non-Darcian analytical simulations seem to correlate well with the maximum pressure drop (i.e., ΔP_{MAX}). Since the isothermal analytical simulation gives a larger pressure drop than the adiabatic one, the isothermal assumption is conservative. The analytical results based on Darcy's law do not correlate as well with the test data as the non-Darcian formulation, even though material properties and conditions were chosen to maximize the pressure drop. The results of this paper indicate that the one-dimensional response of the fibrous insulation of the thermal barrier to rapid pressure transients can be adequately simulated by Eq. 8.

Table 2 gives ΔP_{∞} , the pressure difference between P1 and P4 when the test rig should be at equilibrium. ΔP_{∞} should have been approximately 0 at the end of the recorded data. As Table 2 shows, it was not. This discrepancy was attributed to adjustment problems of the pressure transducers. The precise effect of the non-zero ΔP_{∞} on the maximum pressure drop experienced during the test is not known. However, a low estimate of ΔP_{MAX} may be

reasonably assumed to be

$$\Delta P_{MAX, \text{ low estimate}} = \Delta P_{MAX, \text{ test data}} - \Delta P_{\infty} \quad (11)$$

Table 2 presents important test data characteristics and case results of the analytical simulation. The dP_4/dt rates given in Table 2 are the maximum values recorded for each transient. Per Eq. 8 or 9, ΔP_{MAX} should increase with increasing values of dP_4/dt . However, Table 2 showed that this is not always the case because of the duration of the maximum dP_4/dt . For example, HV8 had a dP_4/dt of 28.3 MPa/s for the first 10 ms, which then decreased to 5 MPa/s during the next 40 ms; for HV6, dP_4/dt was 11.1 MPa/s for 25 ms and 5.2 MPa/s for the next 25 ms. The average dP_4/dt for the first 50 ms of HV8 was 9.7 MPa/s, which is close to the value of 8.3 MPa/s for the first 50 ms of HV6. Therefore, the maximum pressure drops for the non-Darcian analytical simulation depend on the magnitude and duration of the depressurization rates.

Figure 4 compares the pressure response of the fibrous insulation during case HV15 with the non-Darcian analytical results. As Fig. 4 shows, the analytical simulation predicts lower pressures than were recorded during the test. This could be attributed to either a variation of the empirically derived C' or the compression of the fibrous insulation against the perforated tube during the depressurization. The C' was assumed to be a constant for all cases considered. Parametric variation of C' could be used to attempt better correlation with the test data. Such work was not feasible during this study.

APPLICATIONS OF EQ. 8 TO A TYPICAL THERMAL BARRIER COVERPLATE

The techniques which were empirically verified in this study estimated the pressure load on the

TABLE 2
COMPARISON OF TEST DATA AND ANALYTICAL SIMULATION

Case(a) No.	P ₀ (MPa)	(dP ₄ /dt) _{MAX} ^(b)	Test Results				Analytical Results of ΔP_{MAX}					
							Non-Darcian(c) Formulation		Darcy's Law(d) Formulation			
			ΔP_{MAX} (MPa)	Time of ΔP_{MAX} (ms)	ΔP_{∞} (MPa)	$\Delta P_{MAX} - \Delta P_{\infty}$ (MPa)	Isothermal		Adiabatic		Isothermal	
							(MPa)	Time (ms)	(MPa)	Time (ms)	(MPa)	Time (ms)
HV8	1.08	28.3	0.23	~20	0.05	0.16	0.23	12	0.22	12	--	--
HV12	2.06	78.6	0.49	25 to 40	0.17	0.33	0.48	14	0.44	14	0.30	12
HV13	4.00	26.2	0.65	75 to 100	0.30	0.35	0.65	30	0.58	30	0.20	30
HV14	4.01	57.2	0.90	50 to 75	0.30	0.60	0.85	50	0.75	50	--	--
HV15	4.00	80.0	1.16	50 to 75	0.30	0.90	0.95	30	0.85	30	--	--
HV6	1.08	11.1	0.17	~50	0.03	0.15	0.19	26	0.17	26	0.14	26
HV10	2.06	20.0	0.36	~70	0.15	0.24	0.45	50	0.38	50	--	--

(a) HV1 is the case number of individual EIR test.

(b) Maximum depressurization rate given in the EIR data for P4. Estimated to be the maximum depressurization rate of flow out of fibrous insulation.

(c) Non-Darcian formulation is based on $\dot{u} = (-C'/\rho)/(\partial P/\partial x)$.

(d) Using $K = 2.32 \times 10^{-12} \text{ m}^2$. This value appears approximately an order of magnitude lower than most data would indicate. However, it was assumed low so that the calculated ΔP_{MAX} would be closer to the test results.

Fig.4 Comparison of test data and analytical results for case HV15

thermal barrier during a turbine rotor failure from an assumed 142% overspeed accident. This postulated transient has the highest depressurization rate (i.e., ~77 MPa/s) for the HTGR-GT and is, therefore, the controlling depressurization transient for the thermal barrier.

The total pressure load (ΔP_T) acting upon the coverplate is the sum of the pressure drops across the fibrous insulation, seal sheet, and coverplate, as given by Eq. 1.

Solving Eq. 8 with TAC2D for a 159.0-mm (6.25-in.) thick fibrous insulation, the maximum pressure drop due to the fibers yielded $\Delta P_{FI} = 0.59$ MPa. This pressure load is an order of magnitude larger than the sum of ΔP_{SS} and ΔP_{CP} , as estimated in another study. The resulting total pressure drop ΔP_T is sufficiently high to control the design of the thermal barrier.

CONCLUSIONS

This study has verified with test data that, for rapid depressurization, the relationship

$$u = \frac{-C'}{\rho} \frac{\partial P}{\partial x} \quad (12)$$

is more applicable than Darcy's law for predicting the one-dimensional motion of a gas as it escapes from a fibrous insulation. When Eq. 12 was combined with the continuity equation and an assumption of isothermal flow was made, Eq. 8 resulted.

When Eq. 8 was solved for cases representative of experimental conditions, the predicted pressure response of the insulation correlated well with the test data. However, notably, this correlation was only for air with a single initial temperature. This implies that during the test the viscosity (μ) of the gas would have remained constant. Consequently, a viscosity relationship possibly should be included in Eqs. 12 and 8.

The application of Eqs. 8 and 1 in evaluating the thermal barrier of the HTGR-GT has shown that the pressure drop induced by the venting of the fibrous insulation during a rapid pressure transient

is more controlling than the pressure drop due to the flow resistance of the coverplate or seal sheets. The loads induced by the most severe pressure transient for the HTGR-GT were estimated. These loads were found to be high enough to control the thermal barrier design in the regions of the primary loop with severe depressurization rates.

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