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A Small-Scale Enclosure for Characterizing the Fire Buildup Potential of a Room

W. J. Parker and B. T. Lee

Center for Fire Research
Institute for Applied Technology
National Bureau of Standards
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Prepared for
Ship Damage Prevention and Control
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CONTENTS

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W. J. Parker and B. T. Lee

A 0.76 by 0.76 m (30 by 30 inch) enclosure with a 0.61 m (24 inch) high ceiling was used to model some fires in a 3 x 3 x 2.4 m (10 x 10 x 8 ft) burnout room. Temperatures, oxygen concentrations, air velocity, and conductive and radiative heat fluxes were measured. The highest average air temperature in the upper part of the room was taken as a measure of the fire buildup potential of the room. Upper air temperatures attained in the model were similar in most cases to those in the full-scale compartment. From energy balance considerations this air temperature was related to the oxygen depletion in the room and was shown to correlate well with the oxygen content of the combustion gas and air exhausting from the model and full-scale room fires.

Key words: Fire growth; fire tests; flashover; room fires; scale models; thermal radiation.

1. INTRODUCTION

A major concern at the present time in the fire research is the questionable ability of laboratory test methods to measure the fire hazard of building materials under actual use conditions. The difficulty is probably not so much in the standard test methods used, as in their interpretation and application and in the lack of a suitable analytical model for the fire buildup dynamics in rooms or compartments of fire origin. Ideally, a test method should quantitatively determine a relevant fire property of the material such as ignitability, heat release rate, or flame spread rate under meaningful simulated exposure conditions. A procedure for predicting the fire buildup in a room, would then use the information obtained from appropriate laboratory tests together with a fire growth model derived from basic combustion principles. Such a procedure could help formulate more meaningful standards.

A complete analytical description of the fire growth in a room is too complex for exact solution. It is then necessary to make many simplifying assumptions and approximations which require that the analysis be performed in conjunction with experiments on room fires.

Full-scale room fire tests are expensive and time consuming. A prediction method could be developed more economically by predicting room fire behavior in a laboratory size enclosure provided only that the fires in the small size enclosure simulate those in the full size room; i.e., the major phenomena are the same and have roughly the same degree of importance. This correspondence could be verified if approximate scaling relationships could be established between the room and the small enclosure.

In order to predict the magnitude of fire severity there must be some quantitative measure of the level of fire buildup in a room. A suitable measure appears to be the temperature of the hot air layer below the ceiling. The hot air layer, as the term is used in this paper, includes the flaming and nonflaming gaseous pyrolysis and combustion products. When this upper air temperature reaches 500 °C there is rapid pyrolysis and ignition of all combustibles in the upper part of the room. When it exceeds 700 °C there is sufficient radiation transmitted from the heated upper surfaces into the lower part of the room to cause ignition of all combustible materials. This condition is sometimes referred to as flashover. The above critical temperatures are based on observations during the course of these experiments.

The fire buildup potential of a room can, therefore, be considered as the highest temperature that the hot air layer might achieve during the course of a fire.

This paper discusses a simplified prediction method, tests in an experimental small-scale enclosure and a comparison of tests conducted in both a full-scale room and the small-scale enclosure.

2. PREDICTION MODEL

The upper air temperature in a room of given size and enclosing surfaces of prescribed physical and thermal properties can be estimated by means of an overall energy balance for the enclosed space. In this treatment we are only concerned with the growth of the fire up to the time of flashover. The room is assumed to be divided into two temperature regions with the higher air temperature, T_g, in the upper part of the room and the ambient air temperature, T_o, in the lower part of the room. Furthermore, there is a continuous inflow of cool air into the lower portion of the room and gaseous combustion products exhausting from the upper portion through a single open doorway. There is no other opening (door or window) or forced ventilation. Under quasi-steady state conditions the heat balance equation for the room can be written as

Rate of heat generated	=	Rate of heat convected through the opening	+	Rate of heat absorbed by the interior surfaces	+	Rate of heat radiated through the opening	(1)

The rate of heat generated by the combustible contents ("fuel") is an effective value which is more readily determined for gaseous and liquid fuels than for solid combustibles for which the effects of volatilization, melting and charring are somewhat uncertain. If the mass flow rate and heat capacity of the exhausting combustion products are assumed to be the same as those of the incoming ambient air, the convection loss through the opening may be expressed as DCV $(T_g - T_o)$, where D is the density of the ambient air; C is the heat capacity of air at the temperature T_g; and V is the volumetric flow rate of the incoming air which may be supplied by the ventilation system or be induced by the fire. The error in this assumption decreases as the amount of available (excess) air increases, and is much less important for gaseous and liquid fuels. The amount of heat absorbed by the interior linings is a function of the temperature difference, $(T_g - T_s)$, where T_s is the temperature of the hot surfaces in the upper portion of the enclosure. However, it is assumed in this model that the heat flux to the boundary surfaces in the upper part of the room is approximately proportional to the temperature rise of the upper air, $(T_g - T_o)$, with a constant of proportionality, L. Radiation from this upper air layer to the room surfaces is neglected because of the assumed low emittance of the air, and so the heat transfer from the hot air to the enclosure is assumed to be confined to the upper walls and ceiling. The boundary of this heated region is defined by the depth of the hot air zone. Then the rate of heat absorbed by the interior linings becomes $LA_u(T_g - T_o)$ where A_u is total area of the hot upper surfaces. The rate of heat generated within the enclosure is QB, the product of the effective heat of combustion of the burning material and the mass burning rate, respectively. If the radiation loss through the doorway is neglected, eq (1) becomes

$$QB = DCV(T_g - T_o) + LA_u(T_g - T_o) \quad (2)$$

The heat loss from the hot air to the upper surfaces of the room is equivalent to the sum of the conduction heat loss through the upper walls and ceiling, and the radiation loss from the latter surfaces to the lower part of the enclosure.

When steady-state conditions are approached the conduction loss through the interior coverings can be represented by the heat conduction equation,

$$L_c(T_g - T_o) = \frac{K_u}{X}(T_s - T_b) \tag{3}$$

where K_u is the thermal conductivity of the solid lining materials in the upper part of the room, T_b is the temperature of the unexposed (outside) surface of the lining material, X is the thickness of the material, and L_c is the heat loss coefficient by conduction.

If the surface temperature approaches the critical value needed for flashover, the radiation loss from the upper surfaces to the lower part of the room becomes dominant. It is assumed that the temperature in the lower part of the room remains at T_o. Then, the radiation loss can be expressed by

$$L_r(T_g - T_o) = f\varepsilon\sigma\,(T_s^4 - T_o^4) \tag{4}$$

where L_r is the heat loss coefficient by radiation, f is the view factor between the hot surfaces in the upper walls and ceiling and the exposed surfaces in the lower part of the room, ε is the emittance of the room surfaces, and σ is the Stefan-Boltzmann constant.

From eqs (3) and (4) one can obtain the following expression for L,

$$L = L_c + L_r = \frac{K_u}{X}\left[\frac{T_s - T_b}{T_g - T_o}\right] + \frac{f\varepsilon\sigma}{T_g - T_o}\left[T_s^4 - T_o^4\right] \tag{5}$$

This expression for L is used in conjunction with eq (2) or its further development to estimate the maximum temperature that could be reached in a particular room.

Equation (2) can be rearranged to give the temperature rise:

$$(T_g - T_o) = \frac{QB/(DCV)}{1 + LA_u/(DCV)} \tag{6}$$

Assuming no side reactions in the combustion process, eq (6) can be expanded to yield

$$(T_g - T_o) = \frac{(B/B_s)(QB_s/DC_oV)(C_o/C)}{1 + (C_o/C)(L/DC_o)(A_u/A)/(V/A)} \qquad (7)$$

where B_s is the stochiometric burning rate, C_o is the heat capacity of the air at ambient temperature, and A is the floor area. Equation (7) contains some important groupings as illustrated in eq (8):

$$(T_g - T_o) = \frac{Y\Theta^* \quad (C_o/C)}{1 + (C_o/C)(L/DC_o)(A_u/A)/(V/A)} \qquad (8)$$

where B/B_s is identified as the fraction of the oxygen depleted from the incoming air and is denoted by Y, and the quantity (QB_s/DC_oV) is a characteristic temperature rise, Θ^*. Y has a maximum value of unity and can be measured during the course of the room fire tests.

The ratio of the heated area to the floor area, A_u/A, will vary with the room configuration, and the location and severity of the fire. For a 3 x 3 x 2.4 m high room with the heated air layer extending down to one-half of the ceiling height, it would be 2.6. For larger rooms it will be less than this. It is a variable that needs to be examined as part of the study.

V/A is a ventilation parameter equal to the volumetric inflow divided by the floor area. For forced air ventilation this would be specified by the designer. For open doorways it would be proportional to the $WH^{3/2}$ factor, where W and H are the width and height of the doorway, respectively.

For ease of calculation, Θ^* is based on methane. Substituting the calculated values of Θ^* and C/C_o for air into eq (8), the temperature rise for the freely burning fire in a room will be given approximately by

$$(T_g - T_o) = \frac{2470Y}{1 + 705L\ (A_u/A)/(V/A)} \qquad (9)$$

The numerical values used for calculating the characteristic temperature rise, Θ^*, are

$$Q = 46.8 \times 10^3 \text{ J/g for methane,}$$

$$D = 1.32 \times 10^{-3} \text{ g/cm}^3,$$

$$C_o = 1.01 \frac{J}{g°C}$$

and

$$B_s/V = 0.751 \times 10^{-4} \text{ g/cm}^3$$

for 9.52 volumes of air needed to burn one volume of fuel. C/C_o is 1.07 at the 700 °C temperature assumed for flashover. Equation 9 separates the expression for temperature up into convenient factors which can be measured individually during the room fire tests.

Y is measured as the oxygen depletion fraction and can be expressed as

$$Y = \frac{B}{B_s} = \frac{BQ}{DC_oV}\frac{1}{\Theta^*} = \frac{1}{DC_o\Theta^*\ (V/A)} \sum_i q_i (A_i/A) \tag{10}$$

The summation which is over all of the combustibles involved in the fire, focuses down on the fire test requirements. The heat release rate per unit area, q_i, must be obtained with a heat release rate calorimeter, and the area involved in the fire, A_i, requires information from some kind of flame spread test. Both q_i and A_i depend on the radiation field and this should be reflected in the test methods. Whether there is an i-th term depends on the ignitability of the material which is the basis for yet another test.

This expression displays some scaling possibilities. The ratio of the involved area to the floor area, A_i/A, should be preserved. This requires geometrical scaling since the i-th combustible may be a wall. However, (V/A) must also be preserved so one must make adjustments of the dimensions of the openings to secure the proper airflow. The height above the doorway is critical to the phenomena taking place in the room so the height has been scaled geometrically, and $WH^{3/2}$ has been controlled by changing the width of the doorway.

The scaling rules used in this work are as follows:

1. All dimensions are proportional to the scale factor except for:

    a. the width of the doorway which is proportional to the square root of the scale factor, and

    b. the thickness of the materials which remains the same.

2. Fuel content and fuel surface area are proportional to the floor area.

3. Air supply rate is proportional to the floor area.

4. Time is the same in the model as in full-scale.

3. EXPERIMENTAL DETAILS

A quarter-scale model was built of a 3 x 3 x 2.4 metre (10 x 10 x 8 foot) burnout room with a 2 metre high and a 0.9 metre wide (80 x 35 inch) doorway. The doorway width was reduced by one-half. The model was made of steel and lined with low-density asbestos cement board (ACB).

Thermocouples were embedded in the ACB to measure the heat conduction into the walls and ceiling. The floor was independently supported on a load cell to measure the burning rate of the combustible contents. The other variables measured were the vertical temperature profiles in the center of the model and in the doorway, the oxygen and CO concentration of the outgoing air, the horizontal component of the inflow air velocity profile, and the radiation levels incident on the floor. Along with the radiometer on the floor, crumpled-up newsprint served as an ignition indicator.

A series of corner tests were run inside of the full-scale burnout room [1].[1] Two 1.2 x 2.4 metre (4 x 8 foot) wall panels of the specimen material formed one corner in the rear of the room. The ceiling above the corner was lined with another 1.2 x 2.4 metre panel of the same or a different material. Various fire exposure sources were used including wood cribs and waste containers. The variables in the test series were the type of wall material, ceiling material, and fire source.

[1]Numbers in brackets refer to the literature references listed at the end of this paper.

Two of the above tests were repeated in the model, which used a simulated wood crib in the form of a gas burner. The burner was a steel box with a mineral wool cover to even out the flow. The gas flow was adjusted to produce one-sixteenth of the heat release rate of the 6.4 kg (14 lb) wood crib based on a value of 15.1×10^3 J/g (6,500 Btu/lb) for wood. The area of the top surface of the burner was one-sixteenth of that of the wood crib so that the fuel flow rates per unit area were the same.

With this simplified type of scaling we are mainly concerned with trying to reproduce upper air temperatures as closely as possible. Figure 1 shows a comparison of the time histories of the air temperature at 1 inch from the ceiling and at the mid-height of the room for full-scale and model enclosures with lauan walls and gypsum board ceiling.

The maximum inflow air velocity which occurs near the bottom of the doorway should be proportional to the square root of the height of the doorway, therefore, the full-scale velocities have to be divided by two for comparison with the model. Figure 2 shows this inflow velocity profile at the doorway for enclosures having both gypsum board walls and ceiling. The velocities were measured with a hot wire anemometer in both cases.

The similarities have been encouraging on a very limited number of tests. We made some comparisons with more materials in the early part of the project using the IITRI scaling [2] in which the height of the compartment and the height of the opening were proportional to the 2/3 power of the scale factor. The agreement was not as good in that case [3].

The model enclosure was next applied to a realistic fire risk situation. The prototype compartment fires were those conducted under the Navy's habitability program of making berthing spaces more comfortable for the crew without an accompanying significant increase in the fire risk potential.

The series of nine full-scale tests shown on table 1 was conducted in the 3 x 3 x 2.4 metre (10 x 10 x 8 foot) burnout room in which the contents consisted of a three-man bunk with bedding and a three-man locker stuffed with cotton waste. The bedding included a neoprene mattress, cotton sheets, a wool blanket and a chicken feather pillow. The lining materials and ventilation conditions were varied from test to test. Ignition was by 800 ml of ethyl alcohol in the middle of the lower bunk. The bedding materials were in considerable disarray in order to promote as rapid a growth as possible of the fire, so to produce the worst conditions.

Table 1. Navy Compartment Fire Tests

Test	Lining Variable	Door	Air* Supply	Bunk
1	Standard Set	Closed	Off	Closed
2	Standard Set	Closed	On	Closed
3	Standard Set	Open	At Bunks	Closed
4	Standard Set	Open	On	Open
5	High Density Acoustical Panels on Overhead	Open	On	Closed
6	Melamine Coated Panels on Bulkhead	Open	On	Closed
7	Wool Carpet and Pad on Deck	Open	On	Closed
8	Wool Carpet and Pad on Deck	1/2 Open	Off	Closed
9	Wool Carpet and Pad on Deck and Curtains Over Bunk Openings	1/2 Open	Off	Closed

*At ceiling vent unless otherwise specified.

STANDARD SET OF LININGS

Low Density Acoustical Panels on Overhead
Fiberglass Insulation on Two Bulkheads
Vinyl Coated Panels on Two Bulkheads
High Temp Polyamide Carpet Bonded to Steel Deck

These tests were all duplicated in the small-scale model using a scaled-down bunk and locker. The bedding was reduced in area by a factor of 16. Scaling requires that the thicknesses stay the same because of the importance of the heat transfer within the material. For example, if incident radiation levels are the same in the model and the prototype as they should be, the thickness must be the same to produce the same surface temperature.

Keeping the materials the same thickness is a great convenience except for the 3-inch thick neoprene mattress which could barely be squeezed along with the other bedding into the space between the tiers of the bunk. This would constrict the airflow by an unacceptable amount. A compromise was made by using a 1-inch thick mattress in the model, feeling that it would be satisfactory at least in the early part of the test.

Table 1 also shows the range of conditions covered by the nine tests. The first four have progressively increased ventilation. In all but the fourth test the bunk had closed ends and back. In that test the back and ends were removed allowing easy flow of air across the bunk. The standard set of materials, which included a high melting temperature polyamide carpet, vinyl coated aluminum bulkhead panels, and a low density acoustical tile overhead, was used on these first four tests. Then the lining materials were varied one at a time. A partial doorway opening and curtains over the bunk openings were also included in the subsequent tests.

Figure 3 shows the temperature history of the upper air as determined by a thermocouple 1 inch down from the center of the ceiling in the model. In the first two tests the door was closed. The forced ventilation was on in the second test. Neither led to temperatures above 200 °C. On the third test with the door open the air temperature reached nearly 500 °C. This temperature would probably have been maintained for a while or even increased if the full mattress thickness could have been used. Except for a localized bunk fire this was not very spectacular.

Figure 4 shows the same three tests in the full-scale room. The first two tests again exhibited temperatures less than 200 °C. The third test barely reached 500 °C in the initial stage but it maintained the high temperature long enough to ignite the cotton waste in the locker in the upper part of the compartment, causing a very severe fire reaching a final maximum temperature of nearly 900 °C.

Qualitatively one might say that the model did not scale well for test 3 (a big fire in the burnout room -- not much in the model). However, both temperatures approached 500 °C which is a danger point. One became fully involved; the other did not. It could have been deduced from the model test result that the potential for flashover was there. This is all that the model is required to do.

Although the scaling relationships including the fixed thickness requirement for the materials suggest that the model would operate in real time, the same rate of flame spread as in the prototype would mean an earlier peak fire involvement in the model. For rapid fire spread upward, this time difference for maximum fire involvement may not be large (see fig. 1). However, for situations where much of the fire spread is along the horizontal plane, the peak fire development could occur much sooner in the small enclosure than in the prototype room. This is evident from figures 3 and 4. The peak temperatures prior to locker involvement, if any, occurred at 6 minutes in the model and 13 minutes in the full-scale for test 3.

Table 2 provides a comparison of the temperatures 1 inch below the center of the ceiling and the oxygen depletion fractions at the top of the doorway between the small- and full-scale tests of all nine situations. The entries exclude the first peak due to loose sheet burning and later peaks due to ignition of the locker materials if they occur. Except for Tests 1, 8 and 4, the agreement is about as good as could be expected. In Test 1 there may have been sufficient air leakage into the closed compartment to raise the full-scale temperature. In test 8 the agreement between full-scale and model was quite good until the model suddenly increased its involvement very late in time. Test 4 was the only one which permitted cross flow between the tiers of the bunk. The reduced spacing in the model due to the thickness of the bedding materials brought the flames in closer contact with the tier above. This would cause higher temperatures on the underside of the above tier and increase radiation back into the bunk, resulting in a more intense fire, which might account for the 80 °C higher temperature in the case of the model.

Figure 5 shows a plot of temperature rise versus oxygen depletion for the nine tests. Only oxygen depletion data corresponding to peak upper air temperatures are shown. However, a data display similar to figure 5 also occurs when the oxygen depletion versus temperature history is plotted for an individual test. Where there were two successive peaks in the temperature history, as in test 9, the oxygen depletion values for both temperature maxima were indicated.

Table 2. Comparison of the Maximum Temperatures and Oxygen Depletions Between the Full-Scale and the Model

Test	Size	Temperature (°C)	Oxygen Depletion (percent)
1	Full	190	3.7
	Small	105	3.5
2	Full	170	7.8
	Small	165	8.0
6	Full	200	8.5
	Small	213	10
5	Full	232	14
	Small	203	13
7	Full	244	15
	Small	250	12
8	Full	270	13
	Small	560	22
9	Full	402	24
	Small	397	27
3	Full	506	40
	Small	463	23
4	Full	578	39
	Small	660	50

The open circles denote model data and the solid circles denote full-scale data. This plot demonstrates the effect of the thermal losses. The solid line is the theoretical curve assuming no heat losses while the dashed line is the theoretical curve taking radiative heat losses into account with certain assumptions. It assumes that all losses are by radiation into the lower part of the compartment. It uses a typical ventilation parameter of 5 cm/s measured for the open door situation. The product fA_u/A is assumed to be equal to unity as it would be if the surface had an emissivity of unity. For the assumed emissivity of 0.8 it will be only slightly less. With these assumptions the theoretical relationship between Y and $(T_g - T_o)$ can be written as

$$Y = \frac{(T_g - T_o)}{\theta^*} \left(\frac{C}{C_o}\right) \left[1 + \frac{\varepsilon\sigma}{DC(T_g - T_o)} (T_s^4 - T_o^4) / \left[\frac{V}{A}\right]\right] \tag{11}$$

which reduces to

$$y = \frac{(T_g - T_o)}{\theta^*} \frac{C}{C_o} \tag{12}$$

when the radiation losses are neglected. The numerical values used in the calculation are:

$$\theta^* = 2{,}640\text{K},$$

$$C_o = 1.01 \frac{J}{gC},$$

$$C = 1.01 + 10^{-4}(T_g - T_o) \frac{J}{gC},$$

$$\varepsilon = 0.8,$$

$$T_o = 300\text{K},$$

$$D = 1.32 \times 10^{-3} \text{g/cm}^3,$$

and

$$V/A = 5 \text{ cm/s}$$

4. CONCLUSIONS

The general agreement between the full-scale and model data has demonstrated the usefulness of the model as a tool for characterizing room fire development. It can facilitate the development of a prediction method for fire buildup, which in turn would help establish more meaningful fire standards. Equation (9) provides a useful breakdown of the prediction method for upper air temperatures into separately calculable and measurable factors. The correlation between the temperature rise of the upper air and the oxygen depletion is reasonably good. The temperature versus oxygen depletion plot also provides a means of comparing the factors $(T_g - T_o)$, Y, L, and V/A for consistency.

The main thrust of future studies with the model should deal with principles governing the area of involvement, A_i, which is required in the expression for Y.

6. ACKNOWLEDGMENT

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Table 3. Nomenclature

A	floor area
A_i	burning area of i-th combustible
A_u	area of heated surface in upper part of room
B	mass burning rate
B_s	mass burning rate required to consume all of the incoming air
C	heat capacity of air at the temperature T_g
C_o	heat capacity of air at ambient temperature
D	density of ambient air
f	view factor between the hot surfaces in the upper part of the room and surfaces in the lower part of the room
H	height of the doorway
K_y	thermal conductivity of materials lining upper part of the room
L	effective heat transfer coefficient equal to ratio of heat losses per unit area and the temperature rise, $(T_g - T_o)$
L_c	heat loss coefficient by conduction
L_R	heat loss coefficient by radiation
Q	net heat of combustion of burning materials
q_i	heat release rate per unit area of ith combustible
t	time
T_b	temperature of unexposed surface of lining material
T_g	temperature of the hot air in the upper part of room
T_o	ambient temperature of air

Table 3. Nomenclature (cont'd)

T_s	temperature of hot surfaces in upper portion of room
V	volumetric flow rate of incoming air
W	width of doorway
X	thickness of walls and ceiling
Y	fraction of oxygen depleted from the incoming air
ε	emissivity of surface
θ^*	characteristic temperature rise of air in upper part of room $\theta^* = QB_s/DC_oV$
σ	Stefan-Boltzmann constant

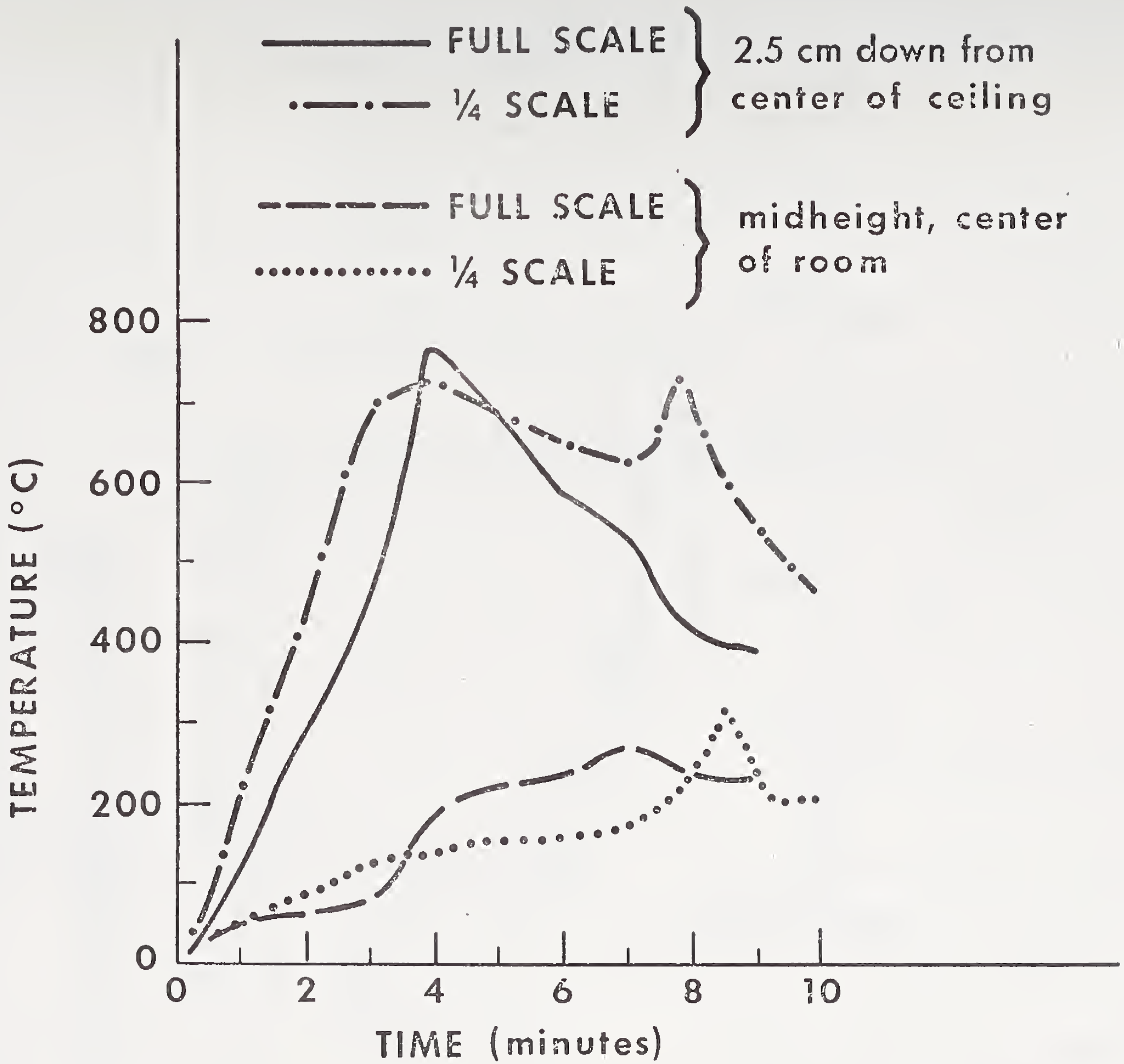

FIG. 1 AIR TEMPERATURES INSIDE COMPARTMENT—LAUAN WALLS, GYPSUM BOARD CEILING

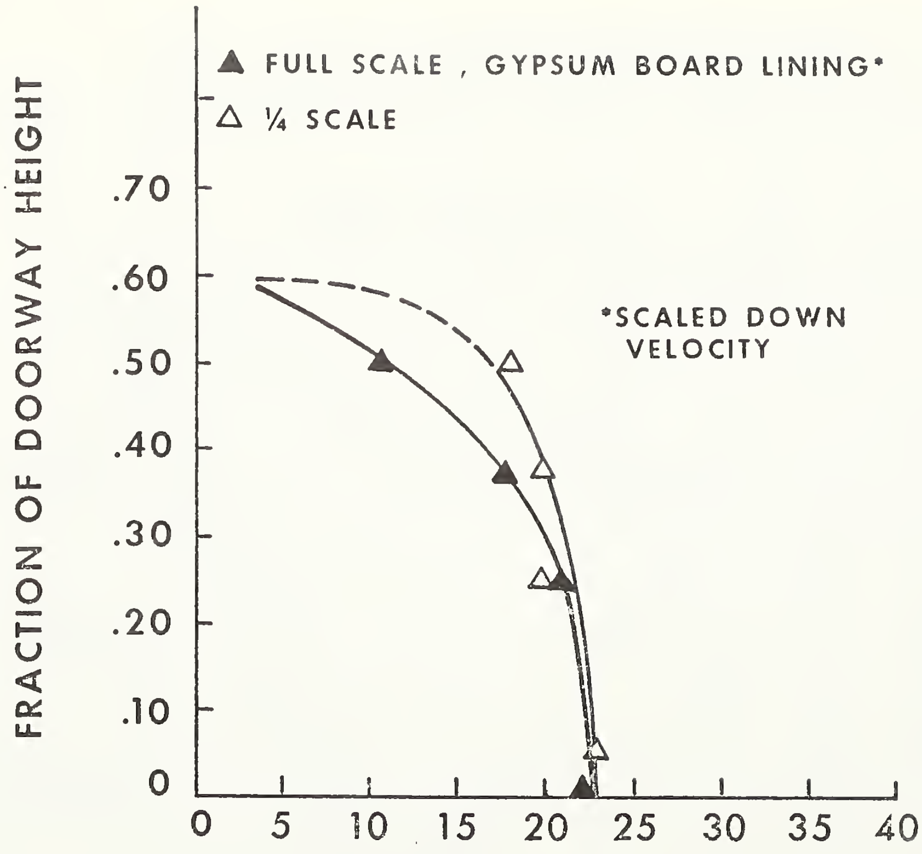

FIG. 2 AIR INFLOW VELOCITY (cm/sec)

Velocity Profile in Doorway at 10 Minutes for Full and Quarter Scale Enclosures. The velocity data for the full scale test is scaled down by the square root of the ratio of the doorway heights.

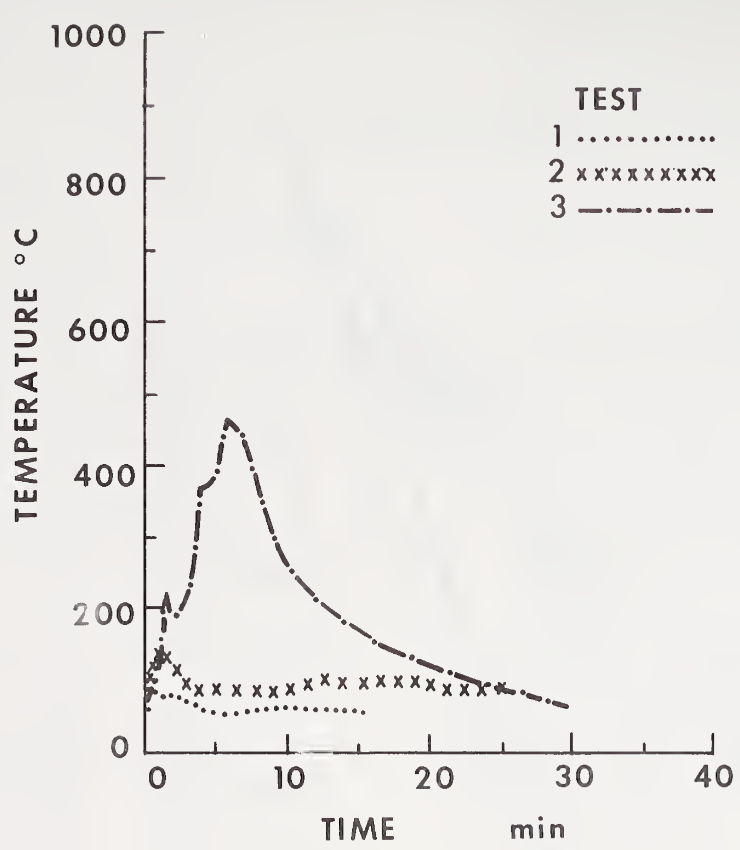

FIG. 3 ONE–FOURTH SCALE COMPARTMENT FIRE TESTS

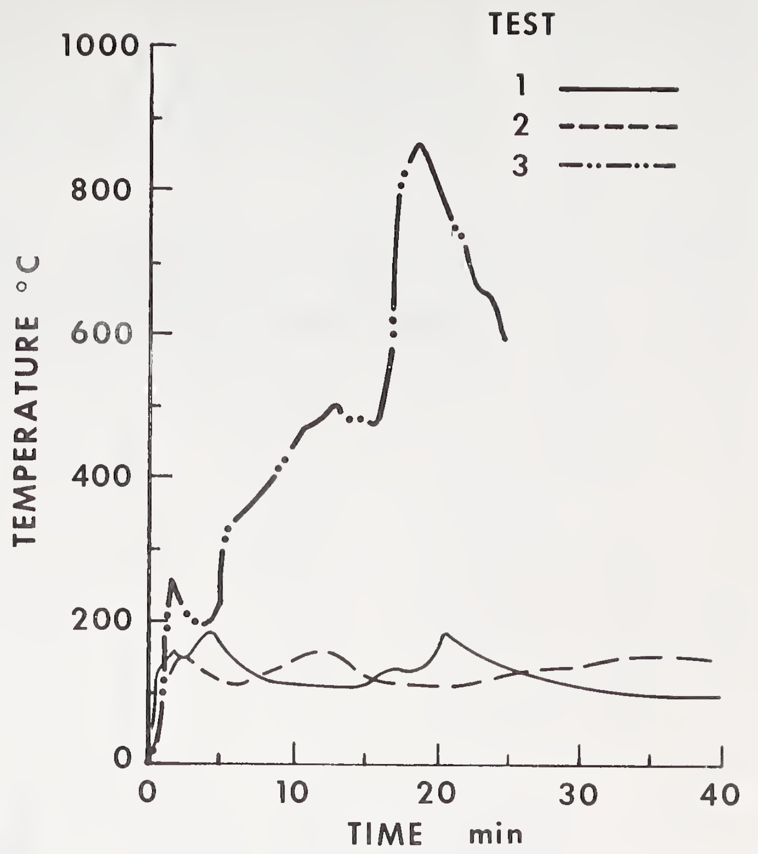

FIG. 4 FULL SCALE NAVY COMPARTMENT FIRE TESTS

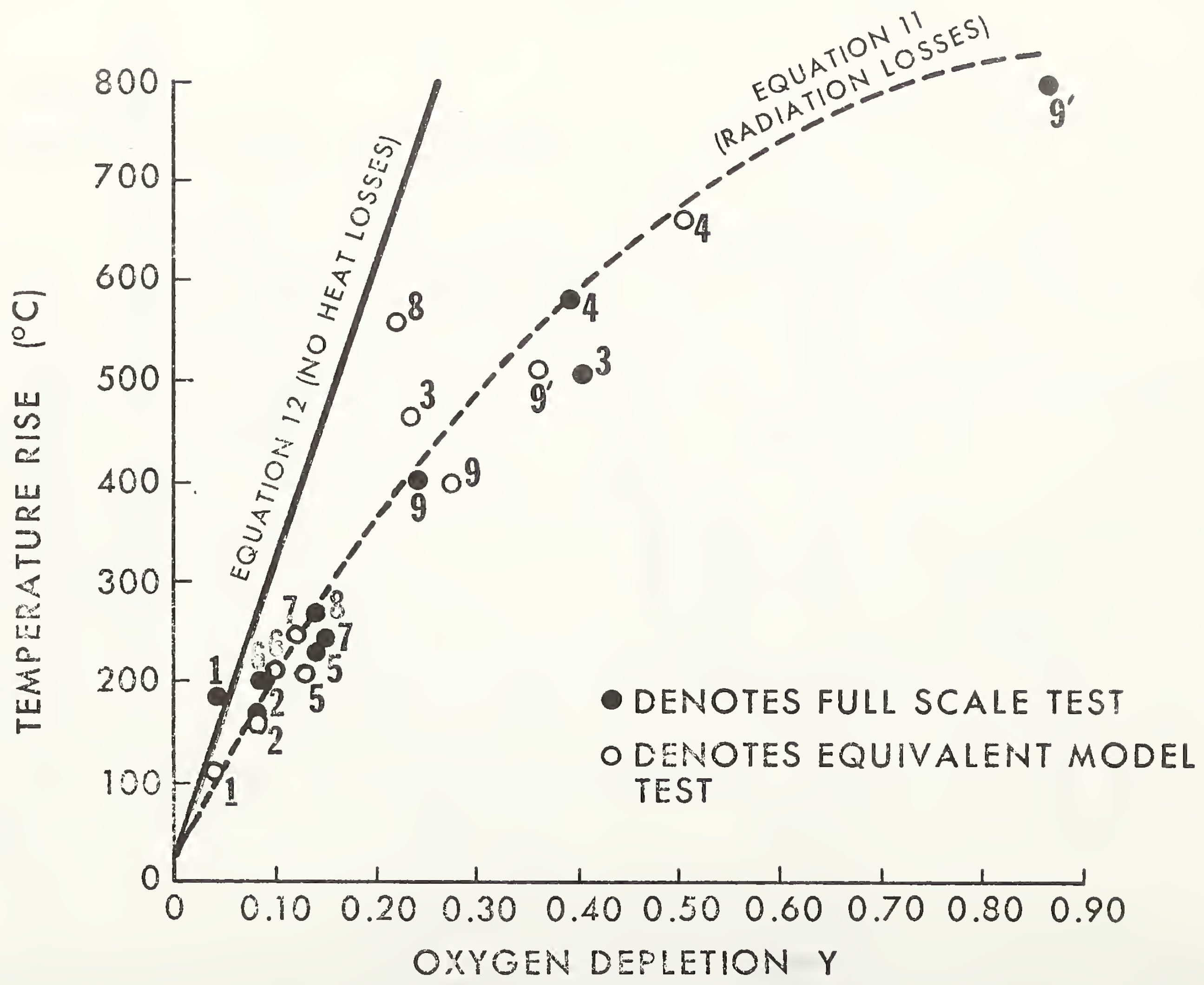

FIG. 5 UPPER AIR TEMPERATURE VERSUS OXYGEN DEPLETION

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15. SUPPLEMENTARY NOTES Order No. N00024-69-F-S177: Contract No. 6101C-NN531

16. ABSTRACT *(A 200-word or less factual summary of most significant information. If document includes a significant bibliography or literature survey, mention it here.)*

A 0.76 by 0.76 m (30 by 30 inch) enclosure with a 0.61 m (24 inch) high ceiling was used to model some fires in a 3 x 3 x 2.4 m (10 x 10 x 8 ft) burnout room. Temperatures, oxygen concentrations, air velocity, and conductive and radiative heat fluxes were measured. The highest average air temperature in the upper part of the room was taken as a measure of the fire buildup potential of the room. Upper air temperatures attained in the model were similar in most cases to those in the full-scale compartment. From energy balance considerations this air temperature was related to the oxygen depletion in the room and was shown to correlate well with the oxygen content of the combustion gas and air exhausting from the model and full-scale room fires.

17. KEY WORDS *(six to twelve entries; alphabetical order; capitalize only the first letter of the first key word unless a proper name; separated by semicolons)* Fire growth; fire tests; flashover; room fires; scale models; thermal radiation.

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