Strength of Cold-formed Steel Studs Exposed to Fire

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Abstract

Building codes in the U. S. and Canada often require that wall panels in residential, commercial, and industrial applications perform structurally for a rated time period under standard fire-test conditions defined by the American Society for Testing and Materials (ASTM) in specification ASTM E119. The American Iron and Steel Institute's Sheet Committees sponsored the present study as part of Project 1202-192, "Fire Resistance of Residential Steel Components," with the major objective of obtaining generic ratings for wall systems with cold-formed thin-walled steel studs. Correlative objectives were to develop an analytical method of predicting the structural behavior of the steel studs and to define possible improvements in the ASTM-E119 fire-test standard.

As part of this study, tension and stub-column (compression) specimens, taken from extra studs supplied for the earlier fire tests, were tested at room and elevated temperatures up to 1200°F (649°C). Parameters developed from the component tests were used to correlate the results of seven wall-panel tests previously sponsored by AISI and three panel tests previously sponsored by U. S. Steel. The results show that the structural behavior of wall panels with thin-walled cold-formed steel studs exposed to ASTM E119 fires can be predicted with reasonable accuracy for temperatures up to 1200°F. The assumptions made for these predictions will be verified by further studies.

This paper describes some of the results of the tests conducted and the parameters and assumptions necessary for the proposed analytical method to predict the structural/time behavior of sheet-steel studs in walls exposed to the ASTM E119 fires.

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Introduction

During the last decade, wood studs commonly used in residential wall construction have been increasingly replaced by steel studs (columns), which are cold-formed from sheet steel. A specification[1)*] was developed over several decades for the design of such columns to withstand dead, live, and/or wind loads at seasonal temperatures. Some building codes in the U. S. and Canada require that the wall structure also be capable of withstanding a specified type of fire with very high temperatures for a given period of time. The standard fire test is described in the ASTM E119 Specification[2)] and may expose a wall structure to temperatures up to 1850°F (1010°C) during a two-hour test. Although this standard has been increasingly criticized for its weaknesses and deficiencies, it was used here to determine how long the tested panels would withstand the fire without a structural failure.

To eliminate the need for a fire test of each different panel produced by the industry, an analytical method to predict the structural behavior of sheet-steel studs exposed to ASTM E119 or similar fires is needed. The design specification mentioned earlier was considered usable with modifications to predict results for the studs used in a fire test, if the following

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* See References.

were known for the encountered range of temperatures: (1) the mechanical properties of the steel used for the studs, (2) the strength-reduction factor Q (as defined in the Specification) for the particular stud configuration used, (3) the lateral stud deflection at midheight, and (4) the average stud temperature at the time of failure.

Tension tests were performed to obtain preliminary values for the mechanical properties. Stub-column tests of short stud lengths were performed to determine the strength-reduction factor. The lateral deflection at time of failure was determined from seven fire tests sponsored by AISI and three fire tests previously sponsored by U. S. Steel. To use the appropriate mechanical and strength-reduction factors in determining the stud strength for a particular test, the average stud temperature must be known as a function of time. This time-temperature relationship was also derived from the panel tests. The results were used, with the specification formula,[1)] to predict the failure time of various panels. Similar steels from three different manufacturers were used in the study.

A detailed account of the study is given elsewhere;[3)] this paper will deal mainly with the analytical method developed to predict the structural/time behavior of sheet-steel studs in walls exposed to the ASTM E119 fire. Also, some of the test results and the parameters and assumptions necessary to perform the analysis will be described.

Description of Fire Test

A brief description of the standard fire test follows; more details are described in ASTM E119.[2] During the fire test, a 10-foot-high (3.05 m) and 10-foot-wide assembly (Figure 1) that simulates the actual wall construction is loaded with the vertical "design load." Wind loads, which were assumed in the design to act laterally on the wall assembly, are not included in the test. The time-temperature relationship of the fire is defined by the standard and is controlled during the test as closely as possible. Specifically, at any given time, the area below the actual time-temperature curve (derived from an average of at least 8 temperature locations in the fire chamber) is not allowed to deviate more than 10 percent from the area under the specified time-temperature curve. However, the uniformity of temperature within the fire chamber is not specified, and very little instrumentation is required. Therefore, little is known about the extremely complex time-temperature and time-displacement relationship of a given steel stud within a tested wall.

In a typical fire test, the average temperature in the fire chamber increases as a function of time, and a temperature gradient develops across each wall element, including the steel studs, Figure 2. During the initial phase of the test, the temperature gradient across the steel studs steadily increases, that is, the flange of the stud near the fire chamber (inside flange) is hotter than the flange near the ambient air (outside flange). This results in a lateral (horizontal) midheight deflection of the wall assembly towards the fire chamber. During the latter phase of the test, some of the gypsum board near the fire chamber starts to crack or flake off. This exposes parts of the wall interior to the fire temperature, and the temperature gradient in adjacent studs reduces rapidly, while the remaining studs may still retain a high temperature gradient. Also, the average temperature of the individual studs increases at different rates. Consequently, towards the end of a fire test, the abovementioned parameters (temperature gradient, lateral deflection, average temperature) differ substantially for each stud in the test panel. ASTM E119 does not require recording of any of the abovementioned parameters, nor does it specify an acceptable nonuniformity of the fire temperature, except as mentioned above (area above or below average time-temperature curve).

This general behavior in a test is further complicated by the inability of the interior gypsum to provide lateral support to the studs against buckling about the minor axis, or torsional buckling, when time of failure approaches. Also, the effective restraint against end rotation of the studs provided by the test fixture is unknown. Furthermore, although one or the other stud may fail locally, the remaining columns may still be able to carry the panel design load for some time. Thus, a prediction of the failure-load/time relationship of sheet-steel studs requires some bold assumptions (and future confirmatory tests), as described below.

Proposed Criterion to Predict Failure Loads

Although many complicating factors exist, it is possible to predict the failure load for a fire test with the aid of modified versions of the specification formulas[1)] by making the following assumptions: (1) the gypsum-board cladding on the inside of the wall and the exterior cladding prevent failure by weak axis buckling (buckling parallel with the cladding) or torsional buckling up to the time of failure, (2) the gypsum board cladding does not carry any vertical load, (3) the stress-strain curve is linear up to the yield strength, (4) the test loads are uniformly applied to all studs in the test panel, (5) all studs have equal temperature gradients, horizontal deflections, and average temperatures throughout the entire test.

The proposed load-ratio criterion (LR) is applicable to the three different types of studs tested in the AISI study, shown in Figure 3, or to other similar types, and is defined as

$$LR = P_T/P \tag{2}$$

where P_T is the calculated (predicted) failure load, of a particular stud at an elevated temperature, T, and P is the failure load at room temperature. P could be determined from tests or from calculations. For studs investigated in the AISI study, P was determined from

$$P = \frac{23}{12} F_{a1}A \tag{3}$$

where F_{a1} is the allowable stress at room temperature for axially loaded compression members with shapes not subject to torsional-flexural buckling, and A is the gross cross-sectional area of the stud. The factor $\frac{23}{12}$ is the reciprocal of the safety factor incorporated in the allowable stress. F_{a1} is determined by Section 3.6.1.1 of the AISI specification, with room-temperature values for Q, E, and F_y.

For the type of columns investigated in the AISI study, elastic buckling would be critical if the column length exceeded 13 to 15 feet (4 to 4.6 m) assuming hinged ends, depending on the type of stud under consideration. Since these studs are usually less than 12 feet long, inelastic buckling is critical. Therefore, the proposed criterion will only be developed for inelastic buckling; however, the method of developing criteria for elastic buckling would be the same except that the equations for F_{a1} would be different. Thus,

$$F_{a1} = \frac{12}{23} Q\, F_y - \frac{3}{23} \frac{(Q\, F_y)^2}{\pi^2 E} \left(\frac{KL}{r}\right)^2 \qquad (4)$$

where

Q = strength-reduction factor at room temperature

F_y = actual yield strength at room temperature, ksi

E = modulus of elasticity at room temperature, 29,500 ksi (203,000 MPa)

K = effective length factor for the stud, equal to 1.0

L = length of stud, in.

r = radius of gyration about major axes, in.

Thus, the failure load at room temperature can be calculated as

$$P = \left[QF_y - \frac{1}{E} \left(\frac{Q\, F_y\, L}{2r\pi} \right)^2 \right] A \qquad (5)$$

Equation 5 was evaluated for the three types of studs investigated in this study. The results are shown in Table I.

The failure load at an elevated temperature cannot be determined by Equation 4 because the yield strength (F_y) and modulus of elasticity (E) are known to change as a function of temperature.[4,5] Also, the strength reduction factor (Q) is expected to change because it also is a function of E and F_y, as will be demonstrated later. In addition, as was explained earlier under the description of the ASTM E119 fire test, the studs deflect laterally during the fire test, thus causing bending stresses, which must be considered in calculating P_T.

For members exposed to compression and bending, Section 3.7 of the AISI specification[1)] is applicable. The interaction equation for combined axial and bending stresses at midheight is used:

$$\frac{f_a}{F_{a1T}} + \frac{C_{mx}}{(1 - \frac{f_a}{F'_e})} \frac{f_b}{F_{b1T}} < 1.0 \tag{6}$$

where the subscript T was added to identify variables that must be determined for failure temperature, T, and

f_a = compressive stress due to axial load, ksi

F_{a1T} = allowable stress, ksi, for axial load at failure temperature determined as follows:

$$F_{a1T} = \frac{12}{23} Q_T F_{yT} - \frac{3}{23} \frac{(Q_T F_{yT})^2}{\pi^2 E_T} \left(\frac{KL}{r}\right)^2 \tag{7}$$

$\left(\frac{C_{mx}}{1 - \frac{f_a}{F'_e}}\right)$ = modification factor for bending stresses

f_b = bending stress at midheight at time of failure, ksi

$F_{b1T} = \frac{3}{5} F_{yT}$ = allowable bending stress at failure temperature, ksi

F_{yT} = yield strength at failure temperature, ksi

Q_T = column-strength reduction factor at failure temperature

E_T = modulus of elasticity at failure temperature, ksi

The other parameters are as defined previously. At the time of failure, the bending stress, f_b, at column midheight is equal to $P_T\delta_T/S_x$, where δ_T equals the midheight deflection at time of failure in inches, and S_x is the section modulus about the major axis of the C-section. Since δ_T is the deflection at the time of failure and includes any amplification (Pδ) effects, f_b already includes the amplification of the bending stresses due to the axial load, and the modification factor $C_{mx}/(1 - f_a/F_e')$ is set equal to 1.0. Thus, Equation 6 reduces to

$$\frac{f_a}{F_{a1T}} + \frac{f_b}{F_{b1T}} < 1.0 \qquad (8)$$

In predicting the failure condition of the studs, the allowable stresses F_{a1T} and F_{b1T} should be multiplied by the safety factors of $\frac{23}{12}$ and $\frac{5}{3}$, respectively. Thus, with $\frac{5}{3}$ $F_{b1T} = F_{yT}$, the predicted failure condition is

$$\frac{f_a}{\frac{23}{12}\,F_{a1T}} + \frac{f_b}{F_{yT}} = 1.0 \qquad (9)$$

Since $f_a = P_T/A$ and $f_b = P_T\delta_T/S_x$, the failure condition at elevated temperature reduces to

$$\frac{P_T}{A(\frac{23}{12} F_{a1T})} + \frac{P_T \delta_T}{S_x F_{yT}} = 1.0 \qquad (10)$$

or

$$P_T = \frac{1.0}{\frac{1}{A(\frac{23}{12} F_{a1T})} + \frac{\delta_T}{S_x F_{yT}}} \qquad (11)$$

From Equations 2, 3, and 11, the proposed load-ratio criterion may be expressed as

$$LR = \frac{1}{\left(\frac{1}{F_{a1T}} + \frac{23\ A\ \delta_T}{12 S_x F_{yT}}\right) F_{a1}} \qquad (12)$$

with F_{a1} and F_{a1T} as defined by Equations 4 and 7, respectively.

F_{a1} is a function of Q, and for nonperforated studs, Q can be determined by calculation, using the effective-width approach according to the AISI specification. An attempt was also made to develop an analytical approach to determine the effective width of flat elements at elevated temperatures, and subsequently Q_T, as outlined in Appendix A. As explained later, the developed equations cannot be used until more data are available for parameters F_y and E_T at elevated temperatures.

Parameters for Proposed Criterion

Some of the parameters necessary to determine LR in Equation 6, particularly for the columns investigated in this study, are available from producers' catalogues or specifications (A, S_x, r_x, Q, F_y, E); however, others had to be determined by tests (Q_T, F_{yT}, E_T). Furthermore, a relationship of the stud temperature and horizontal (lateral) deflection as a function of time had to be developed empirically. More details of this procedure are described elsewhere.[3] A brief description is given, as follows.

Material and Geometric Properties

The actual geometric and material properties of the studs used in this study were determined by tests and measurements. The results are briefly described below.

The actual geometric properties determined from available steel studs are shown in Table I and compare very well with those determined from specified dimensions shown in Figure 3. The dimensions used for calculating the actual section properties were determined from numerous averaged measurements.

The actual physical properties of the steel studs were determined for all three types of studs used in this study. Tension tests at room temperature were performed on specimens from each of the three types of steel studs. The average yield strength for each stud type is shown in Table I. Also, specimens from the BSC studs were tested at elevated temperatures as shown in Figure 4. The test results for the yield strength and the modulus of elasticity of cold-reduced sheet steel as a function of temperature are shown graphically as a ratio (F_{yT}/F_y and E_T/E) in Figure 5, which includes the corresponding properties of plate steel for comparison.[4,5] As seen from these results, the yield strength and modulus of elasticity for sheet steel appear to reduce more rapidly with temperature than those of plate steel.

The method of determining the modulus of elasticity was not in accordance with the ASTM E111 specification;[6] therefore, the results must be considered approximate. However, these approximate values provided good correlation between the tests and calculated results[3]

A check of the chemical analysis of the three types of stud materials was made. The results showed that all measured values were within specified limits.

Strength-Reduction Factor

A strength-reduction factor, Q, is used in the current AISI specification formulas[1] for the column strength of cold-formed members to account for local buckling of the flat elements comprising the stud. Normally, this factor is calculated from the mechanical properties and dimensions of the studs. However, the factor could not be calculated for some of the studs under consideration because they contained holes or perforations that are not covered by the specifications. Furthermore, the strength-reduction factor can presently not be determined from the specification for elevated-temperature conditions. Therefore, stub-column tests were performed to determine Q or Q_T, in accordance with currently drafted AISI methods, from the relationships

$$Q = \frac{P_{ult}}{A\ F_y} \tag{13a}$$

$$Q_T = \frac{P_{ult}}{A\ F_{yT}} \tag{13b}$$

in which P_{ult} is the experimental ultimate load, A is the gross cross-sectional area, F_{yT} is the yield strength at the test temperature, and Q_T is the strength-reduction factor at the test temperature. Stub-column tests were performed at room temperature for all types of studs used in this study, and also at two different elevated temperatures for one type of stud. A special heating chamber was designed for these tests, as shown in Figure 6.

The resulting strength-reduction factors at room temperature are shown in Table I. The elevated-temperature factor divided by the room-temperature factor (Q_T/Q) is shown in Figure 5. This figure shows that Q_T reduces with increasing temperature.

Calculated Q values for room-temperature conditions (ignoring holes or perforations) were consistently greater than the experimental Q values, as expected. The best agreement was obtained for the USSC stub column that did not contain a hole. This shows that the current specification provides reasonable values for the intended applications and that the test results for Q or Q_T were reasonable.

Lateral-Failure Deflection
vs. Time Relationship

Lateral deflections of the test panels, especially at panel midheight, develop during the fire tests because the presence of fire on one side of the test panel and ambient air on the other side causes a temperature gradient across each steel stud. Since the fire temperature is a function of time, the lateral displacements are also a function of time. This relationship is complicated by many other factors during the last few minutes of the fire test so that it is difficult to analyze the deflection/time relationship for wall panels with steel studs and gypsum boards near the time of failure. Therefore, the test records of previously conducted fire tests were scrutinized, and the test results were correlated with calculations based on the recorded test data. The tests and the correlation procedures are briefly described below.

Wall-Panel Tests. The results of 10 recently conducted tests on wall panels exposed to the ASTM E119 fire are summarized in Table II. The first seven of these tests were sponsored by

AISI and conducted at Underwriters' Laboratory (UL). UL will soon issue a report on these tests,[7)] which will provide a detailed record of the test temperatures, displacements, and observations recorded during the tests. The steel studs for these wall panels were furnished by Bethlehem Steel Corporation (BSC - 4 panels) and Wheeling Pittsburgh Steel Corporation (WPSC -3 panels). The other tests were previously sponsored by U. S. Steel Corporation (USSC -3 panels). One of the USSC tests was conducted at Ohio State University[8)] and the other two were conducted at UL.[9,10)]

All panels consisted of C-shaped steel studs of varying thickness and dimensions, spaced 2 feet (0.6 m) on centers. Attached to the fire side were one to three layers of 1/2- or 5/8-inch-thick (12.7 and 15.9 mm) gypsum board. Gypsum board or steel siding was attached to the cold side of the panels. In four of the panels, fiberglass insulation was placed between studs and claddings; the other six tests were performed without insulation. More details on the construction of the panels may be found in the appropriate reports.[5,6,7,8)]

Test Correlation. Equation 11 was used to correlate the test load at elevated temperatures, P_{Test}, with a calculated load, P_T. The actual section properties and material properties previously determined were used to calculate P_T, along with the average temperature at the hottest cross section, measured at time of failure, and the lateral midheight deflections, calculated or estimated from previous measurements.

Specifically, hinged end conditions were assumed for each of three midheight deflections investigated: (1) the calculated lateral displacement based on the maximum temperature difference recorded for any stud cross section in a given test panel, (2) a limiting condition represented by lateral displacements assumed to be equal to zero, and (3) an estimated failure-deflection criterion, which is described in more detail below. Unfortunately, no data were taken at the exact time when the wall panels failed. Such data would have allowed a correlation on the basis of measured midheight displacements.

As seen from the correlation results shown in Table II, the estimated-failure-deflection vs. time criterion (Condition 3, above) is the most reasonable correlation approach with all correlation ratios (actual test load divided by calculated load) equal to or greater than 1.0 but less than 1.48. The estimated-failure-deflection vs. time criterion is summarized in Figure 7.

Because of this good correlation, the previously discussed equations were also used to develop the criterion for predicting the behavior of similar wall panels in future tests. The proposed criterion will be described later.

Average Temperature vs. Time Relationship

To predict the failure loads, the average temperature of the studs as a function of time must be known to determine Q_T, F_{yT}, and E_T. The maximum recorded average temperature for the tests shown in Table II, as well as for other previous tests not reported in the literature, follows the time relationship plotted in Figure 8. Also shown in this figure are the individual records for each of the 10 panels described previously (Table II). The use of maximum average temperatures should provide conservative results.

Evaluation of Proposed Criterion

Equation 12 was used to determine the LR/time relationship for various panel types discussed previously. For example, for a given panel type (defining insulation and gypsum panels) and an assumed failure time, M (in minutes), the failure deflection, δ, and the average failure temperature, T, were calculated by use of Figures 7 and 8, respectively. The geometric and material properties at room temperature (A, S_x, r_x, F_y, E, Q) were taken from Table I. On the basis of the calculated T, the material properties at failure temperatures (F_{yT}, E_T, and Q_T) were calculated from Figure 5.

A typical calculation of LR/failure-time curves for a panel with one layer of 5/8-inch-thick (15.9 mm) gypsum board (with and without insulation) is shown in Table III.

The results of the calculations in Table III, and those for other panels, are summarized in Figures 9 and 10. These figures represent the LR/failure-time relationship for the investigated panels with or without insulation, respectively. A horizontal line is shown in both figures at LR = 12/23, which represents the inverse of the safety factor incorporated in the design of the studs. Thus, the intersection of this horizontal line with the LR/failure-time curves represents the predicted test-failure time if the applied load is equal to the design load. Curves above LR = 12/23 and below LR = 0.125 are shown as dashed lines because of uncertainties in these regions, as discussed below and elsewhere.[3)]

Also shown in Figures 9 and 10 are results for the 10 panel tests discussed earlier. The proposed criterion provides a conservative prediction of the behavior of the investigated panels. Therefore, Figures 9 and 10 may be used for easier selection of a panel type that meets the specified need of a design, as demonstrated in an example described in Appendix B.

Limitations of Prediction Method

The suggested method of predicting the failure loads in fire tests of stud walls is heavily dependent on (1) an empirical determination of the variation of the stud temperature during the test, and (2) an empirical determination of the lateral deflection of the stud during the test. Future tests with more comprehensive data recorded at time of failure may confirm the underlying assumptions of these empirical assumptions. Therefore, the method should not be applied to wall-panel construction that is significantly different from that described herein with respect to geometry of the stud cross section, amount of insulation, cladding, and other physical characteristics. These limitations will have to be defined in more detail in the future.

Other limitations were previously indicated by the dashed curves in Figures 1, 4, and 5. For load ratios $LR \geq 12/23$ there is uncertainty as to whether the assumed linear failure-deflection/time relationship represented in Figure 5 is applicable. Future panel tests at higher load ratios should help to resolve this uncertainty. Possible variability of the average-failure-temperature/time relationship for these load ratios, which result in relatively low stud temperatures, has only a minor effect on F_{yT}, E_T, and Q_T. However, for load ratios $LR \leq 0.125$, the average-failure-temperature/time relationship would exceed 1200°F (649°C). To determine F_{yT}, E_T, and Q_T at these temperature levels from Figure 4 would be questionable because the curves above 1200°F have to be substantiated by future tensile and stub-column tests (scheduled for the next phase of this study).

Conclusions

The results of the described study show that the structural behavior of wall assemblies with thin-walled cold-formed steel studs exposed to E119 fires can be predicted with reasonable accuracy for average failure temperatures up to 1200°F (649°C). These predictions were based on the limited information derived from the tension, stub-column, and wall-panel tests described in this report.

Additional tests will be made to determine material properties at elevated temperatures more extensively, including temperatures exceeding 1200°F. Also, some wall-panel tests will be conducted with automated data-taking equipment capable of recording deflections, temperatures, and loads for each stud within the test wall panel.

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Table I

Parameters Used for Calculation of Stud Loads at Room and at Elevated Temperatures

Stud Type	A $in.^2$	r_x in.	S_x $in.^3$	F_y ksi	Q	P k
BSC	0.351	1.413	0.390	54.0	0.684	10.0
USSC	0.351	1.369	0.376	55.2	0.805	11.0
WPSC	0.366	1.425	0.409	51.1	0.678	10.0

A = Gross cross-sectional area, in.

r_x = Radius of gyration about major axis, in.

S_x = Section modulus about major axis, $in.^3$

Q = Strength reduction factor at room temperature.

F_y = Yield strength at room temperature, ksi.

P = Ultimate load at room temperature, k.

E = Modulus of elasticity at room temperature, 29,500 ksi (203,400 MPa).

1 in. = 25.4 mm
1 $in.^2$ = 6.45 cm^2
1 $in.^3$ = 16.4 cm^3
1 ksi = 6.89 MPa

Table II

Wall Panel Test Correlation

Test Panel No.	Wall Description*	Average Test Load Per Stud, P_{Test} k	Time of Failure, minutes	Max. Average Failure Temperature, T	Max. Temperature Difference at Failure δT	C_T**	F_{yT}** ksi	E_T** ksi	For δ Based on on ΔT at Failure: δ in.	P_T k	$\frac{P_{Test}}{P_T}$	Based on $\delta = 0$: P_T k	$\frac{P_{Test}}{P_T}$	Based on Estimated δ***: δ in.	P_T k	$\frac{P_{Test}}{P_T}$
1	WPSC/AISI/UL 2 @ 1/2" No. Ins., SS	3.373	93	1047	120	0.549	19.42	12,690	0.30	2.94	1.15	3.31	1.02	0.09	3.19	1.09
2	WPSC/AISI/UL 1 @ 5/8" No. Ins., SS	2.530	58	1145	60	0.495	12.78	9,440	0.15	1.92	1.32	2.03	1.24	0.06	1.99	1.27
3	BSC/AISI/UL 1 @ 5/8" No. Ins., SS	1.596	85	1240	30	0.454	9.18	5,900	0.06	1.24	1.29	1.27	1.26	0.09	1.23	1.30
4	BSC/AISI/UL 3 @ 1/2" No. Ins., SS	4.560	119	1162	400	0.481 (0.597)	12.42 (25.92)	8,560 (15,930)	1.03 (0.38)	1.32 (3.83)	3.45 (1.19)	4.47	1.02	0.12	4.24	1.08
5	WPSC/AISI/UL 1 @ 5/8" No. Ins., CB	2.530	46	1154	0	0.492	12.26	8,850	0.00	1.94	1.31	1.94	1.31	0.05	1.90	1.33
6	BSC/AISI/UL 2 @ 5/8" No. Ins., SS	4.560	104	930	100	0.607	27.00	16,520	0.24	4.25	1.07	4.71	0.97	0.10	4.51	1.01
7	BSC/AISI/UL 1 @ 5/8" With Ins., SS	4.560	37	740	700	0.663	36.2	21,200	1.62	3.78	1.21	6.68	0.68	1.00	4.54	1.00
8	USSC/USSC/UL 1 @ 5/8" With Ins., SS	1.542	54	1175	450	0.555	11.59	8,260	1.16	1.27	1.21	1.91	0.81	1.46	1.17	1.32

(Continued)

Table II (Continued)

Test Panel No.	Wall Description*	Average Test Load Per Stud, P_{Test} k	Time of Failure, minutes	Max. Average Failure Temperature, T	Max. Temperature Difference at Failure ΔT	Q_T**	F_{yT}** ksi	E_T** ksi	For δ Based on on ΔT at Failure			Based on $\delta = 0$		Based on Estimated δ***		
									δ in.	P_T k	$\frac{P_{Test}}{P_T}$	P_T k	$\frac{P_{Test}}{P_T}$	δ in.	P_T, k	$\frac{P_{Test}}{P_T}$
9	USSC/USSC/OSU 2 @ 5/8" With Ins., SS	2.000	115	1430	550	0.540	9.38	5,900	1.50	0.91	2.20	1.48	1.35	0.23	1.35	1.48
10	USSC/USSC/UL 3 @ 1/2" With Ins., SS	1.746	127	1800	600	0.540	9.38	5,900	1.76	0.85	2.05	1.48	1.18	0.13	1.40	1.25
Mean of All Tests											1.626 (1.400)		1.084			1.210

* Wall description, first line: supplier/sponsor/testing agency.
** Q_T, F_{yT}, and E_T are based on maximum recorded average temperature T.
*** Determined from Figure 7.

Conversion Factors

1 k = 9.81 kN
1 in. = 25.4 mm
1 ksi = 6.89 MPa

Table III

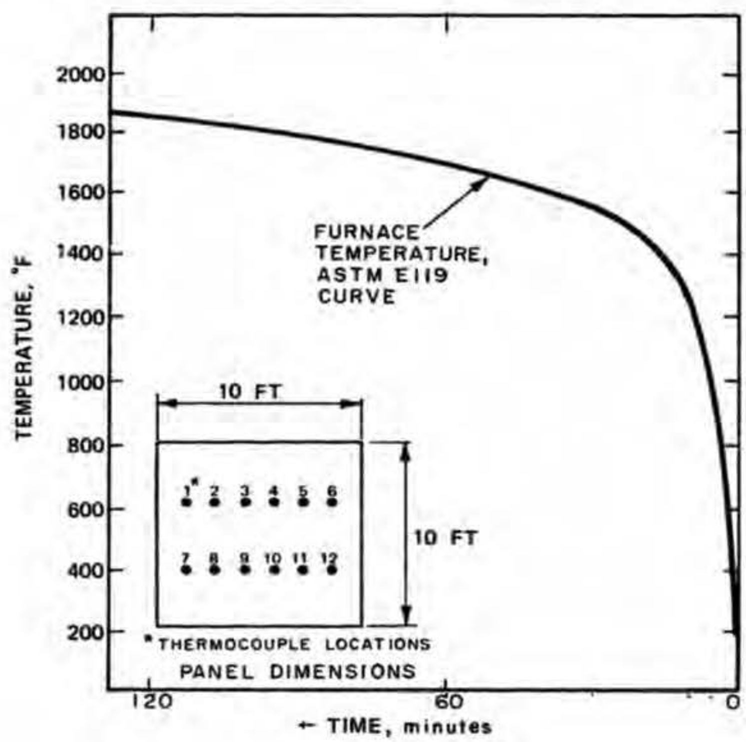
Typical Calculation Summary to Determine Curves for One Layer of 5/8-Inch-Thick Gypsum Board Shown in Figures 9 and 10

M, minutes	5	10	20	30	40	50	60	80
T, °F	125	180	280	550	880	1110	1220	1250
δ, inch (no insulation)	0.005	0.010	0.072	0.030	0.040	0.050	0.060	0.084
δ, inch (with insulation)	0.135	0.270	0.540	0.810	1.080	1.350	1.620	2.160
Q_T/Q	1.0	1.0	1.0	0.99	0.76	0.76	0.67	0.67
E_T/E	1.0	1.0	0.99	0.86	0.36	0.36	0.22	0.20
F_{yT}/F_y	1.0	1.0	0.98	0.82	0.31	0.31	0.18	0.17
Q_T	0.684	0.684	0.684	0.677	0.629	0.520	0.458	0.458
E_T, ksi	29,500	29,500	29,205	25,370	17,405	10,620	6490	5900
F_{yT}, ksi	54.00	54.00	52.92	44.28	29.16	16.74	9.74	9.18
$(23/12)F_{a1T}$, ksi	28.487	28.987	28.001	23.506	14.810	7.401	3.894	3.65
P_T/P (no insulation)	0.998	0.995	0.974	0.813	0.511	0.255	0.134	0.125
P_T/P (with insulation)	0.940	0.886	0.782	0.595	0.348	1.169	0.86	0.076

Notes: Room temperature values used for calculations are shown in Table I for the BSC column (P = 10.0 k)
T - values from Figure 3.
δ - values from Figure 2, or
No insulation: $\delta = 0.001$ M,
With insulation: $\delta = 0.027$ M.
Q_T/Q, E_T/E, and F_{yT}/F_y from Figure 1.
$(23/12)F_{a1T}$ from Equation 6 (with $KL/r_x = 1.0 \times 120/1.413$).
P_T/P from Equations 2 and 5.

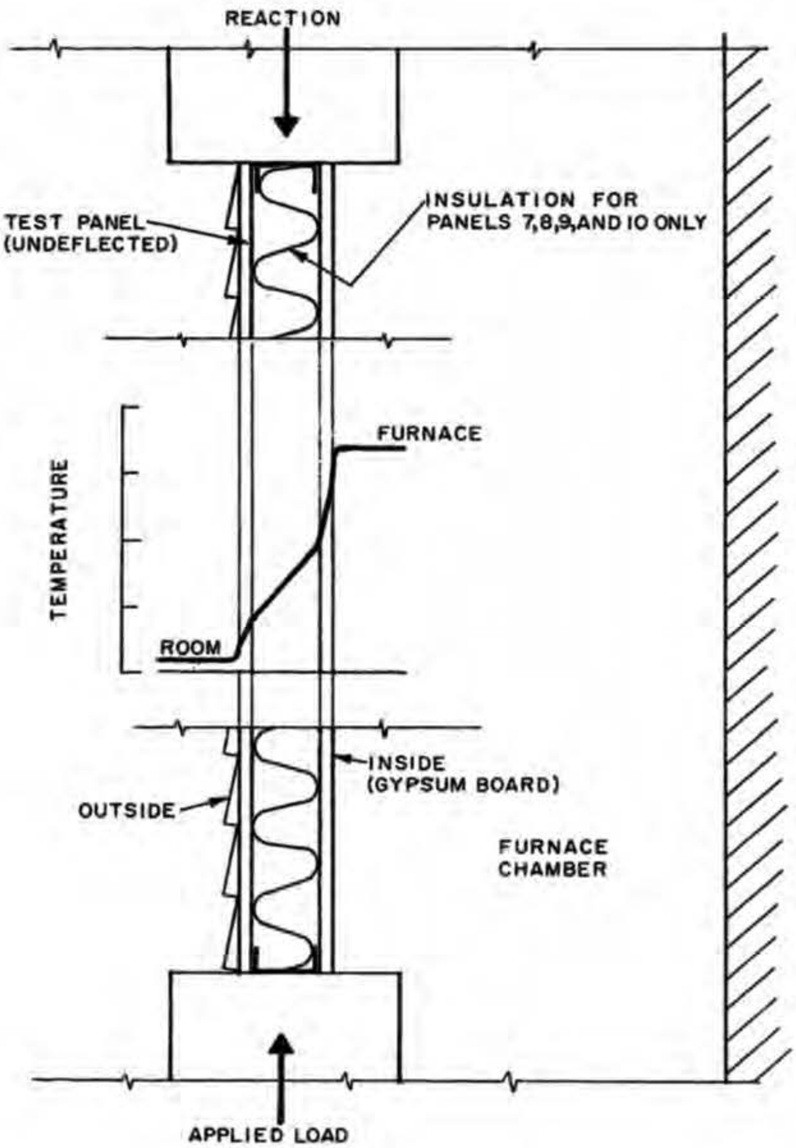
<u>Conversion Factors</u>

1 in. = 25.4 mm
1 ksi = 6.89 MPa
°C = 5(°F - 32)/9



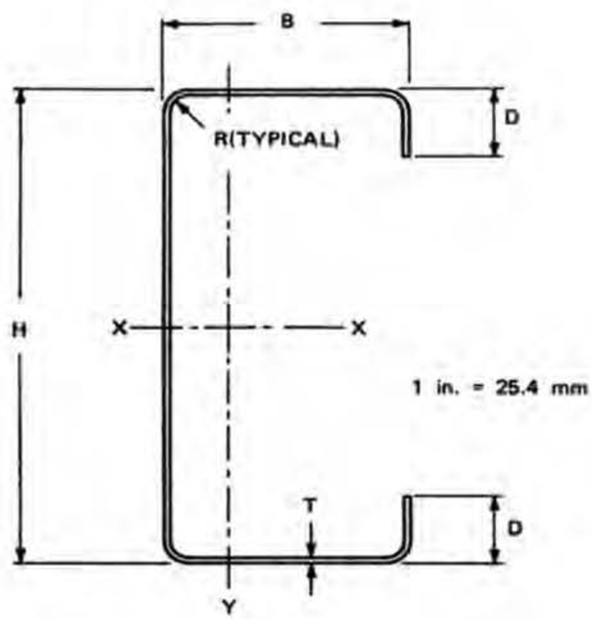
ASTM E119 FIRE TEST

Figure 1



CROSS SECTION THROUGH FIRE TEST SETUP

FIGURE 2



Designation	H in.	B in.	D in.	T in.	R in.
BSC	3.500	1.500	0.3750	0.048	0.048
USSC	3.500	1.500	0.5000	0.050	0.094
WPSC	3.625	1.625	0.5625	0.048	0.094

NOTES: (1) Designations by Suppliers

BSC for Bethlehem Steel Corporation with 0.750-inch-diameter holes at various locations in web element.

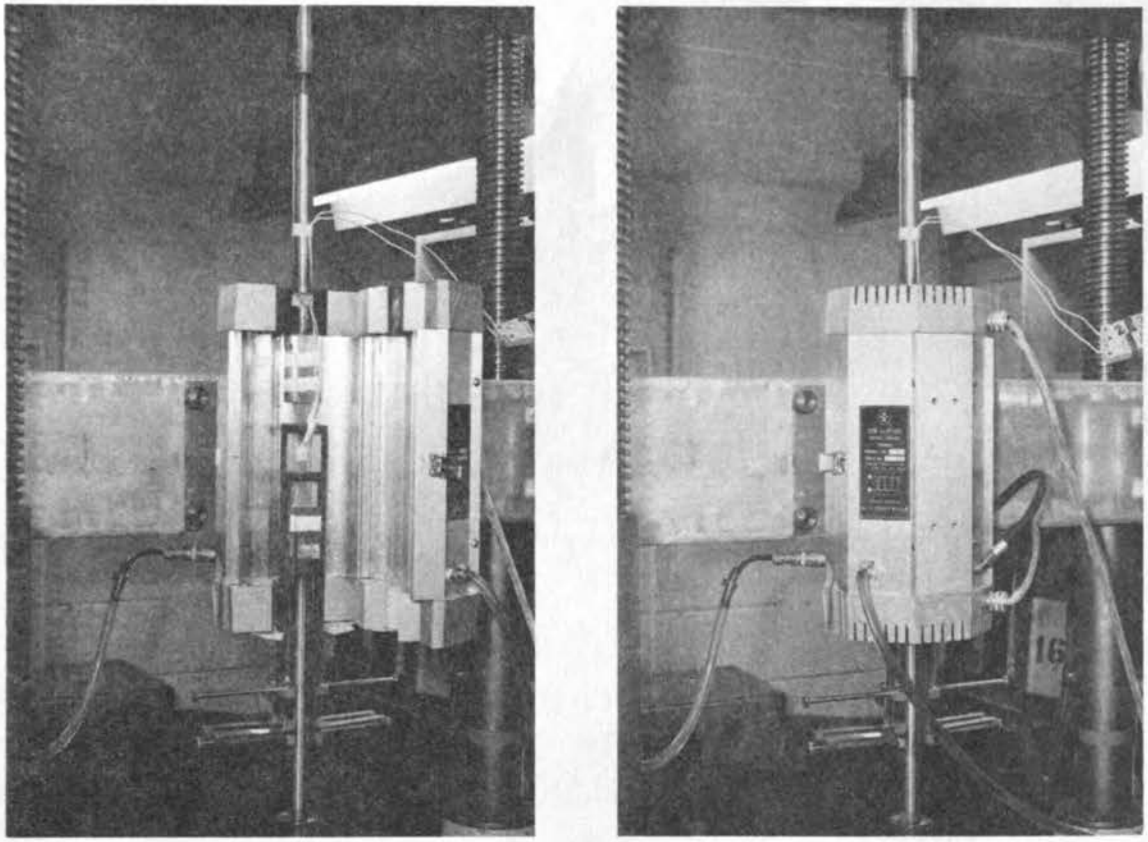
USSC for United States Steel Corporation with 1-1/4-inch-diameter web holes at quarter points

WPSE for Wheeling-Pittsburgh Steel Corporation with 1-3/8-inch-wide by 4-inch-long holes in web, 6 inch cc.

(2) Thickness T is the net thickness, i.e. 0.0018 in. subtracted from measured thickness for commercial coating for BSC and USSC columns.

SPECIFIED DIMENSIONS AND DESIGNATIONS FOR STEEL STUDS

Figure 3



Furnace Open Furnace Closed

Tension Tests at Elevated Temperatures

Figure 4

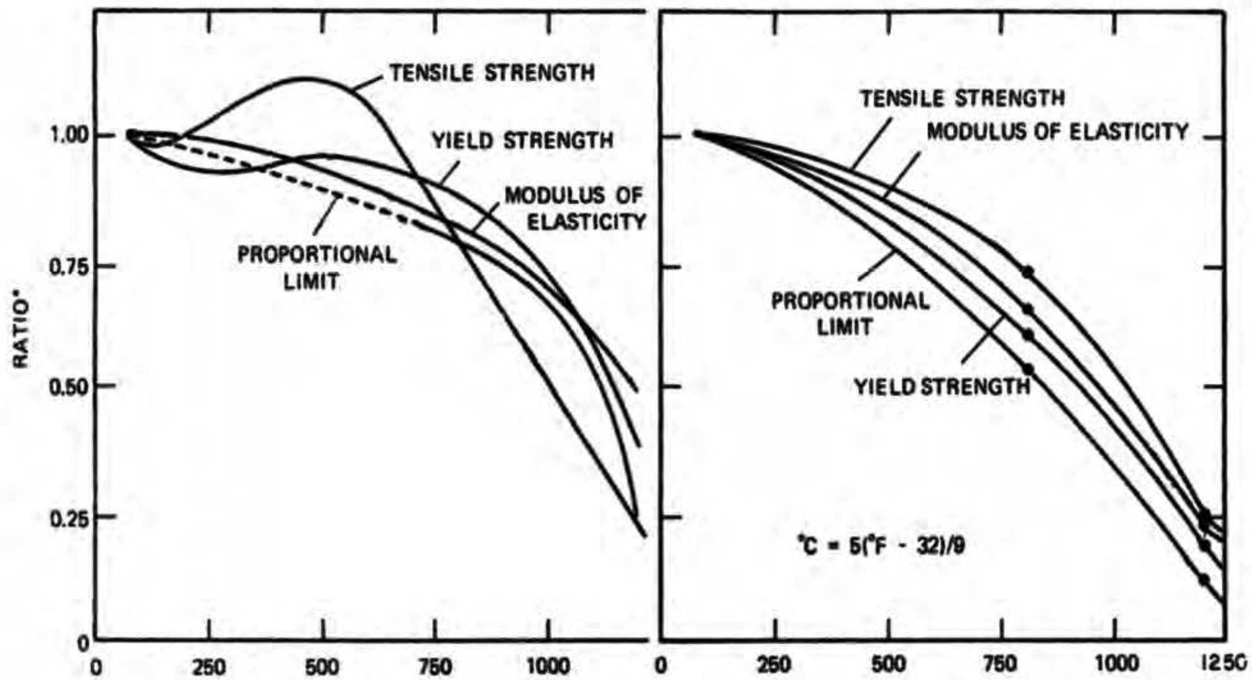


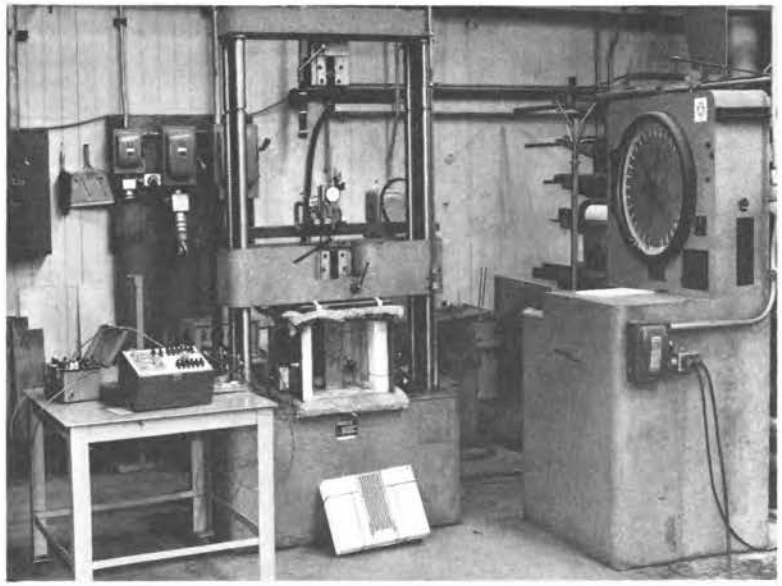
PLATE STEEL, LOW CARBON (A36)

SHEET STEEL, LOW CARBON

* NOTE: RATIO OF PARAMETER AT ELEVATED TEMPERATURE DIVIDED BY THAT OF ROOM TEMPERATURE

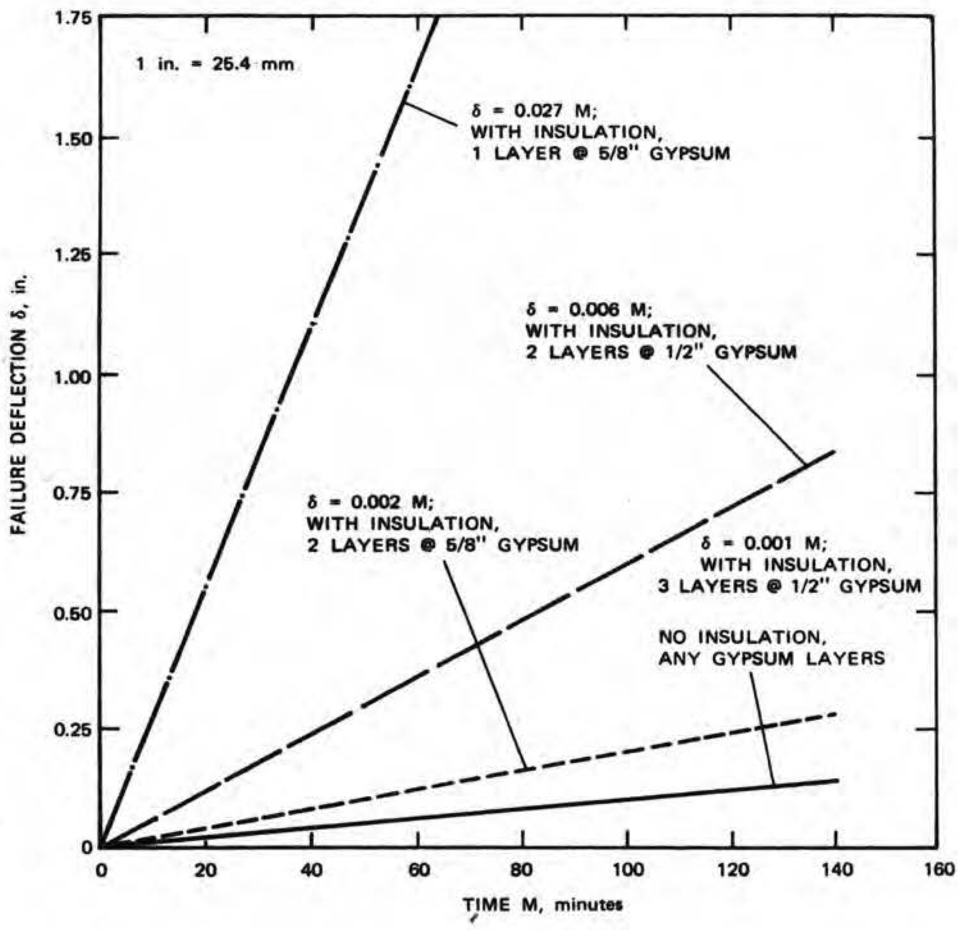
PLATE STEEL VERSUS SHEET STEEL BEHAVIOR AT ELEVATED TEMPERATURES

Figure 5



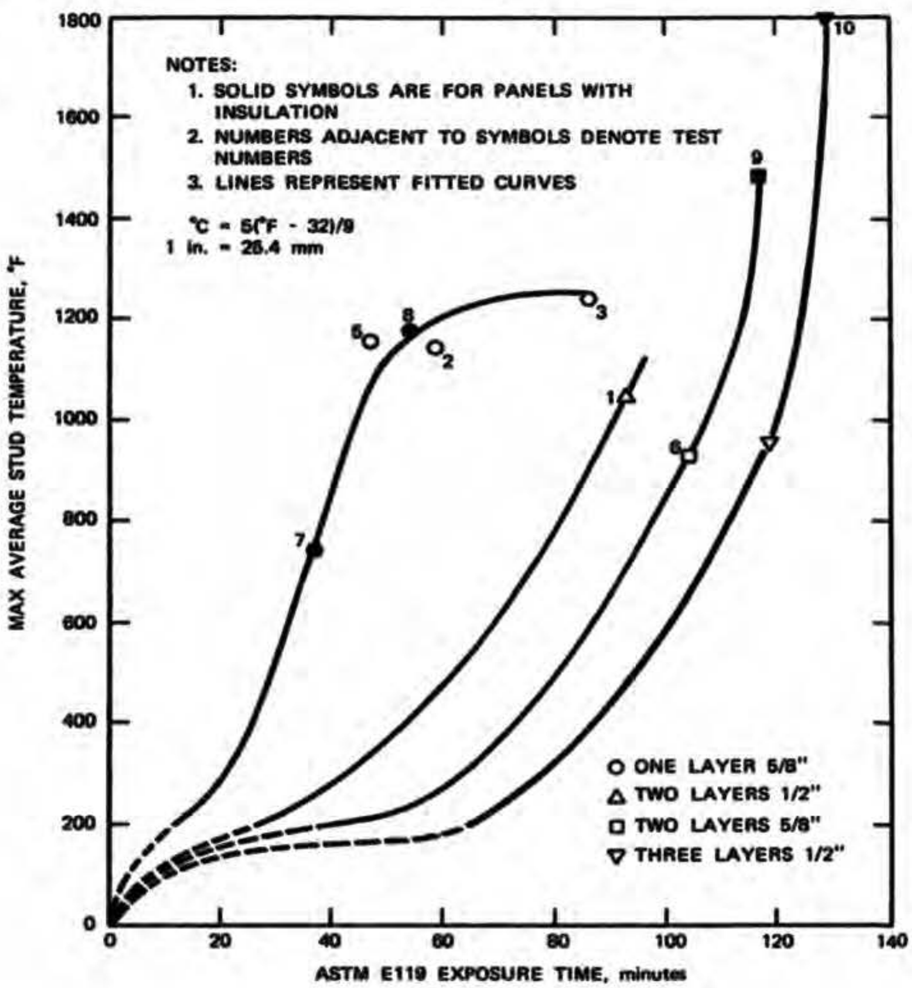
Stub-Column Test Setup

Figure 6



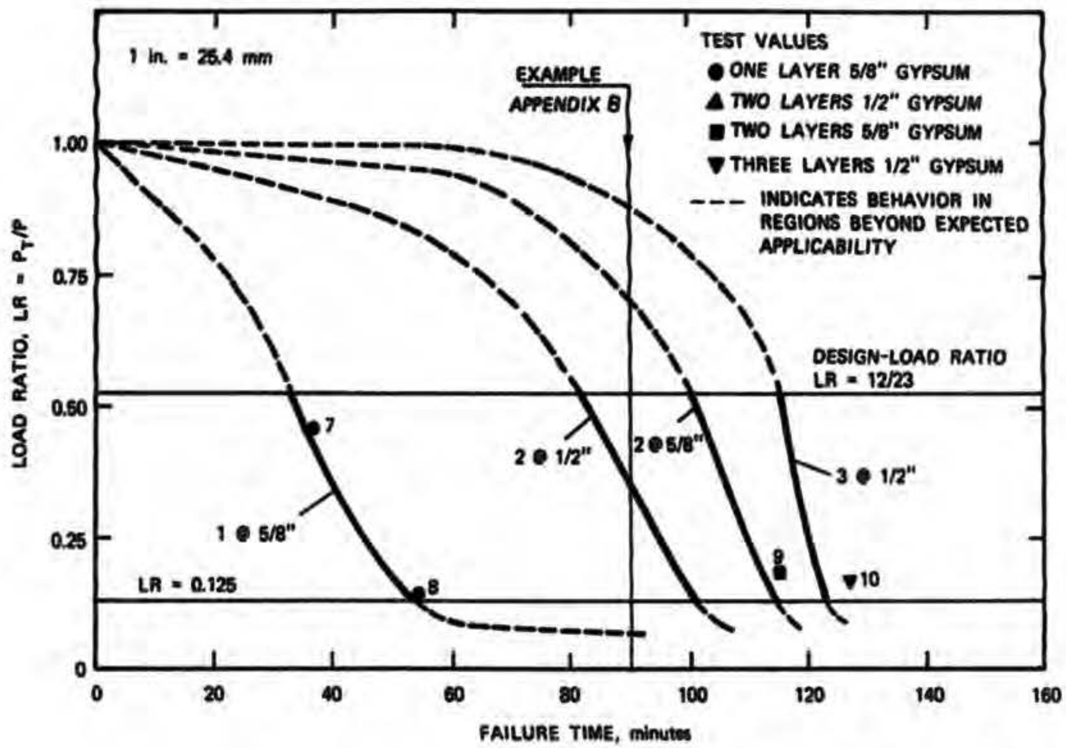
ESTIMATED FAILURE-DEFLECTION/TIME
RELATIONSHIP OF STUDS IN WALL PANELS

Figure 7



AVERAGE FAILURE-TEMPERATURE/TIME RELATIONSHIP OF STUDS

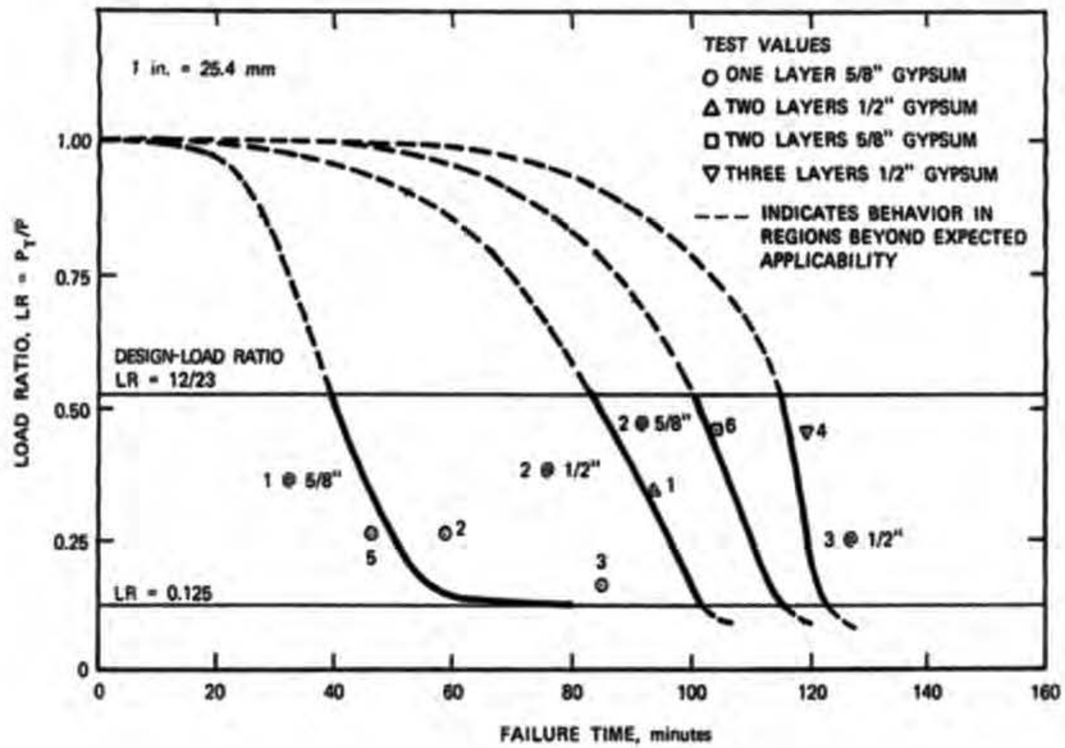
Figure 8



LOAD-VS-TIME RELATIONSHIP FOR WALL PANELS WITH INSULATION

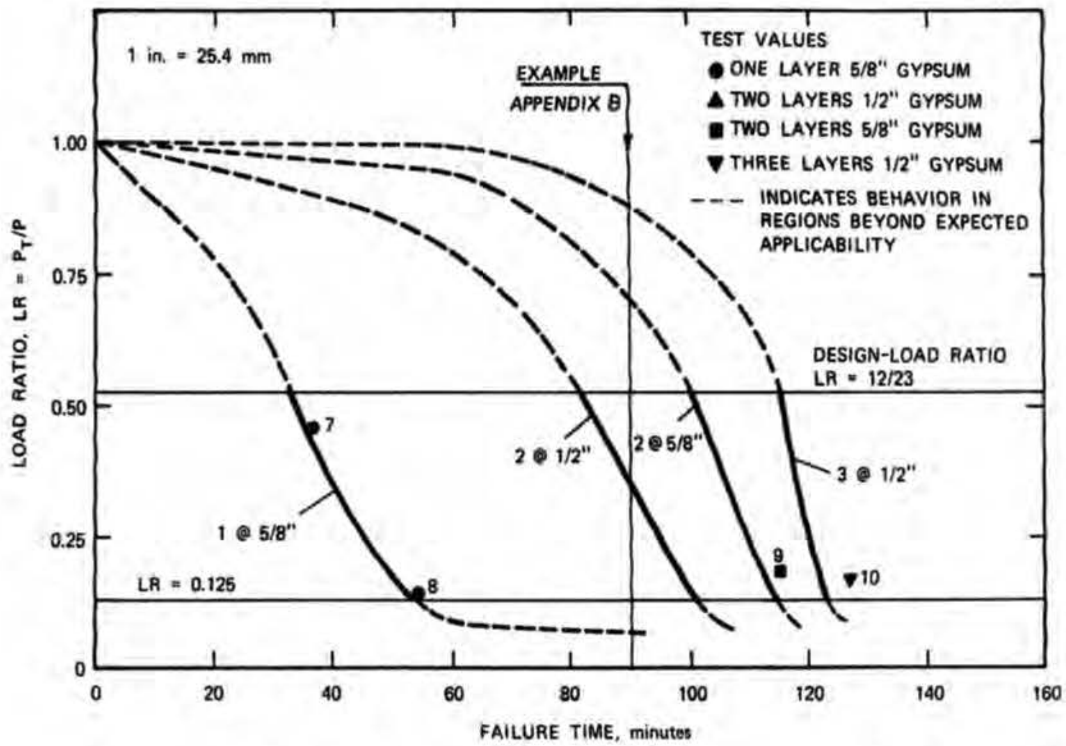
STUDS EXPOSED TO FIRE

Figure 9



LOAD-VS-TIME RELATIONSHIP FOR WALL PANELS WITHOUT INSULATION

Figure 10



LOAD-VS-TIME RELATIONSHIP FOR WALL PANELS WITH INSULATION

Figure 9

Appendix A

Effective Width of Flat Elements at Elevated Temperatures

For stub columns without perforated elements, the strength-reduction factor can be calculated from the mechanical properties by using specification[1)] formulas. The strength-reduction factor, Q, is defined as the ratio of the effective cross-sectional area to the gross cross-sectional area. For the particular shapes investigated in this study, only the web element would have to be reduced. As a result of local buckling the effective width of the web of the studs is less than the actual width, and is given by[1)*]

$$b/t = \frac{253}{\sqrt{f}} = \left[1 - \frac{55.3}{(w/t)\sqrt{f}}\right] \tag{A-1}$$

in which b is the effective width, w is the actual width, t is the thickness, and f is the design stress. This equation can be written in the following more general form,[11,12,13,14)*] which involves the mechanical properties of the steel:

$$b/t = \frac{\pi}{\sqrt{3(1-\nu^2)}}\sqrt{\frac{E_T}{F_{yT}}}\left[1 - \frac{\pi(4.15/4.75)}{4\sqrt{3(1-\nu^2)}\,(w/t)}\sqrt{\frac{E_T}{F_{yT}}}\right] \tag{A-2}$$

---

* See text References.

in which ν is Poisson's ratio and can be taken as 0.3 at all test temperatures.[4,5]* The web is fully effective if

$$w/t \leq \frac{0.678\,\pi}{\sqrt{3(1-\nu^2)}} \sqrt{\frac{E_T}{F_{yT}}} \qquad \text{(A-3)}$$

Equations A-2 and A-3 also apply to the flanges, but for the tested sections the flanges are fully effective.

The lips are considered unstiffened elements and are fully effective when

$$w/t \leq 0.368\,\sqrt{E_T/F_{yT}} \qquad \text{(A-4)}$$

where w is the flat portion of the lip beyond the rounded corner.

By inspection of the previous results, plotted in Figure 5 of the main text, the room-temperature ratio E/F_y would be lower than the elevated-temperature ratio E_T/F_{yT}. This would be so because F_{yT}/F_y is shown to drop faster than E_T/E. As a result, Equation A-2 would lead to a higher effective width, and subsequently to a higher Q value at elevated temperatures than at room temperatures. This is contradictory to the reduction of the Q_T/Q ratio determined by tests and as shown in Figure 5. Therefore, the predicted loads at elevated temperatures, P_T, were based on Q_T determined directly by

* See text References.

stub-column tests. To eliminate such tests for other types of panels, it would be desirable to perform refined tension tests to determine E_T more accurately. This would allow engineers to calculate Q_T from the effective-width relationship expressed in Equation A-2.

Appendix B

Example for the Use of the Proposed Criterion

The usefulness of the proposed criterion, graphically summarized in Figures 9 and 10 of the text, is demonstrated by the following example. It is assumed that a 90-minute rating is desired for a wall panel with insulation. Entering Figure 9 at M = 90 minutes and moving upward, the first intersection is with the curve for a panel with one layer of 5/8-inch-thick gypsum board at a level of LR = 0.07; this choice is discarded because it falls below LR = 0.125 and therefore is outside the usable portion of the curves.

The next intersection occurs with the curve for panels with two layers of 1/2-inch-thick (12.7 mm) gypsum board at LR = 0.33. This may be an acceptable type of panel provided that the resulting predicted failure load, P_T (equal to LR times ultimate load P at room temperature), meets the design criteria of a particular panel application. A higher load, if necessary, may be provided by the next intersection, that is, with the curve for panels with two layers of 5/8-inch-thick (15.9 mm) gypsum board. This intersection is above the maximum LR value of 0.52 permitted by the specifications without regard to fire rating (unless the studs are overdesigned for the room temperature condition). This means that the two 5/8-inch layers are sufficient to provide a fire rating exceeding

90 minutes without a reduction in the design load for the studs. In fact, the chart shows that a fire rating of 100 minutes could be achieved as indicated by the intersection of the horizontal design-load line (at LR = 0.52) with the curve for two 5/8-inch layers. The choice provided by the next intersection, with the curve representing panels with three layers of 1/2-inch-thick gypsum board, would not be considered because it is uneconomical.