Comments on NIST WTC7 Report

Submitted by:

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Comments

1. Unless noted otherwise, all comments submitted pertain to Report NCSTAR 1-9, "Structural Fire Response and Probable Collapse Sequence of World Trade Center Building 7", 2008.
2. The assessment of WTC7 appears to conclude that composite beams are extremely susceptible to failure due to thermal expansion. This is not our experience at all.
3. We do not agree with the calculations on p. 347 indicating shear stud failure. Under the theory presented, without axial restraint at the girder end, the W24 beams try to expand, but this is entirely prevented by the slab, producing very high forces at the shear connectors. In reality, the slab is also heated and expands but more importantly the beam and slab deflect downwards due to differential thermal expansion. This relieves most of the thermal force on the studs.
4. Similarly the LS-DYNA analysis on pp. 349-354 locks in thermal stresses by imposing no translation at all slab edges and no thermal expansion or temperature rise in the slab. Both are unrealistic.
5. We conducted a series of 21 standard fire tests on simply-supported composite beams in the 1980's [1]. These were summarised and the failure times were compared with those calculated based on strength. Excellent correlation was achieved, based on full composite connection. There was no indication that shear stud failure could cause premature failure. However, the beams were 3 m in length not 16 m, but the calculations on p. 347 do not show or imply any dependence on beam length.
6. The proposed method of analysis of WTC7 does not appear to have previously been applied to any known cases, such as standard fire tests or the Cardington fire tests [2] and varies considerably from previous analyses of such tests and experiments.
7. Regarding the large-scale finite element analysis using ANSYS, it appears that the failure criteria for the slab may underestimate the true performance and be responsible for initiating the failure (although failure limits used for the studs are not clear in the report). Concrete strain limits of 0.15% in tension or 0.4% in compression are applied at the slab mid-depth, after which the slab element is completely removed from the grid (p. 487). For slab elements near beams, this removes the lateral support to the beam. When the beam subsequently

buckles, it is also removed from the analysis. Failure then progresses. This is quite unrealistic.

8. The ANSYS analysis appears to have used beam temperatures which are uniform over the beam depth, while the stud temperature is based on the average of the beam temperature and the slab temperature. A temperature gradient in the beam (which actually occurs) may be beneficial, as discussed below. The stud temperature will be affected by the beam top flange temperature but not the web and bottom flange temperatures, so may be overestimated in the analysis.

9. This analysis procedure appears to produce a gross underestimate of strength. In reality, the Cardington testing seems to show that the slab, in conjunction with its connection to the beams, greatly enhances the stability of the floor system, even when cracked and crushed locally. Tensile membrane action in the slab provides a great benefit in performance. Thermally-induced deflections of both the beam and the slab also help, as they promote catenary support mechanisms (including tensile membrane action).

10. Programs such as ABAQUS, VULCAN and ADAPTIC have been used by many researchers to model these effects in the Cardington tests [3,4,5,6]. Reasonable agreement has been obtained, supporting the validity of both the test method and the analysis procedure. We are not aware of ANSYS or LS-DYNA being used and compared with fire tests or other experimental data, although they may also be suitable if used correctly. It should be noted that the modelling of concrete in fire remains a challenging task. Each of the aforementioned programs incorporates a complex algorithm for this calculation, and considerable effort would have to be made to set up a reasonable model in a different program.

11. One factor limiting the performance of the slab in WTC7 would appear to be the very light reinforcement mesh used, being 60 mm^2/m compared with 142 mm^2/m in Cardington and 250 mm^2/m being more typical. On the other hand, the steel deck was insulated (not usual in Australia) and would have provided considerable tensile capacity in one direction. In the direction perpendicular to the deck ribs, there was not much tensile capacity in the slab.

12. The thermal response calculations appear strange. We have used thermal properties derived from our local 3-sided Monokote tests to calculate steel temperatures [7]. We have obtained results similar to those shown in Fig 4.7 (p. 84), indicating that our material may be similar to that used in WTC7. We calculate a fire-resistance period of 67 mins for 0.5 inches and 550°C, not 120 minutes. We assume the difference may be due to the restrained fire testing used in USA, permitting higher steel temperatures. However, the results shown Fig 11.51 (p. 531) apply to the same beam and the same 1100 °C fire temperature but produce much lower temperatures. They do not mention temperature gradient in the beam, so we are not sure whether they have considered the 3-sided exposure. At 60 mins, the 4-thermocouple average temperature from Fig. 4.7 would be about 800 °C, while Fig 11.51 shows about 630°C. On the other hand, Fig 11.52 shows exposure to standard fire conditions, indicating that 0.5 inch produces 550 °C after only 23 minutes. Surely this cannot give 120 mins even when tested restrained.

13. In summary, we do not agree with the conclusions of the analysis. The accuracy of the FDS fire temperatures calculated depends entirely upon the assumptions used. Any chimney effects could have produced much hotter fire temperatures. We have not found any accurate method of predicting fire temperatures in large enclosures, but it appears that more severe

conditions are produced as the distance from the façade to the building core increases. For ventilation, a mid-range condition, with high burning rate but limited heat loss to outside, may be the most severe. In the current case, for a distance of 16 m with mid-range ventilation, very severe conditions may be expected. We therefore believe that the steel beams failed due to reaching much higher temperatures than reported. This resulted from fires which were hotter for longer than calculated and from the small insulation thickness.

14. We understand that there was little physical evidence obtained from this building. In particular, there appears to be no evidence that the composite action between the beams and the slab was detrimental.

References

1. Proe, D J, "Ultimate Strength of Simply-Supported Composite Beams in Fire", thesis submitted for degree of Master of Engineering Science, Monash University, December 1989.
2. Kirby, B et al, "The Behaviour of Multi-Storey Steel Framed Buildings in Fire", British Steel Corporation, 1999.
3. Elghazouli, A Y, Izzuddin, B A and Richardson, A J, "Numerical Modelling of the Structural Fire Behaviour of Composite Buildings", Fire Safety Journal, Vol. 35, p. 279-297, 2000.
4. Huang, Z, Burgess, I W and Plank, R J, "Fire Resistance of Composite Floors Subject to Compartment Fires", Journal of Constructional Steel Research, 2004.
5. Bailey, C G, "Membrane Action of Slab/Beam Composite Floor Systems in Fire", Engineering Structures, Vol 26, p 1691-1703, 2004.
6. Gillie, M, Usmani, A and Rotter, M, "Bending and Membrane Action in Concrete Slabs", Fire and Materials, Vol 28, p 139-157, 2004.
7. Proe, D J, Bennetts, I D, Thomas, I R and Szeto, W T, "Handbook of Fire Protection Materials for Structural Steel", Australian Institute of Steel Construction, 1990

Appendix A : Calculation of Thermal Response of Secondary Beam

Approximate calculations of thermal performance for the W24x55 beam were performed as follows:

- The exposed surface area to mass ratio for the W24x55 steel section was calculated to be 20.9 m^2/t (equivalent to a surface area to volume ratio of 164 m^{-1}), based on three-sided fire exposure. See Fig. A1.
- Using the thermal response chart for Australian Monokote (three-sided fire exposure) extracted from reference 4, it was determined that this beam would reach an average steel temperature of 550°C at 66.7 mins. The average temperature is based on the 4 thermocouple locations used in the fire tests, with two thermocouples on the bottom flange, one at the mid-height of the web and one on the top flange.
- Thermal calculations were performed based on the lumped steel mass approach, ignoring the temperature gradient in the steel and ignoring heat flow into the concrete slab. Using standard fire exposure, the thermal properties of the insulation material were adjusted until an exposed surface area to mass ratio of 20.9 m^2/t and a Monokote thickness of 12.7 mm produced a temperature of 550°C at 66.7 minutes. See Fig. A2.
- Using a fire which was constant at 1100°C, the thermal response was calculated. See Fig. A3.
- The response to the 1100°C fire was compared with that shown in Fig. 4.7. Reasonable agreement was obtained, indicating that the Monokote materials used in Australia and USA may be similar. The thermal response from Fig. 11.51 was added. This showed poor agreement with that from Fig. 4.7, despite being for the same beam size and fire exposure case.
- The response for standard fire exposure was taken from Fig. 11.52 and added to Fig. A2. Again, this showed poor agreement.

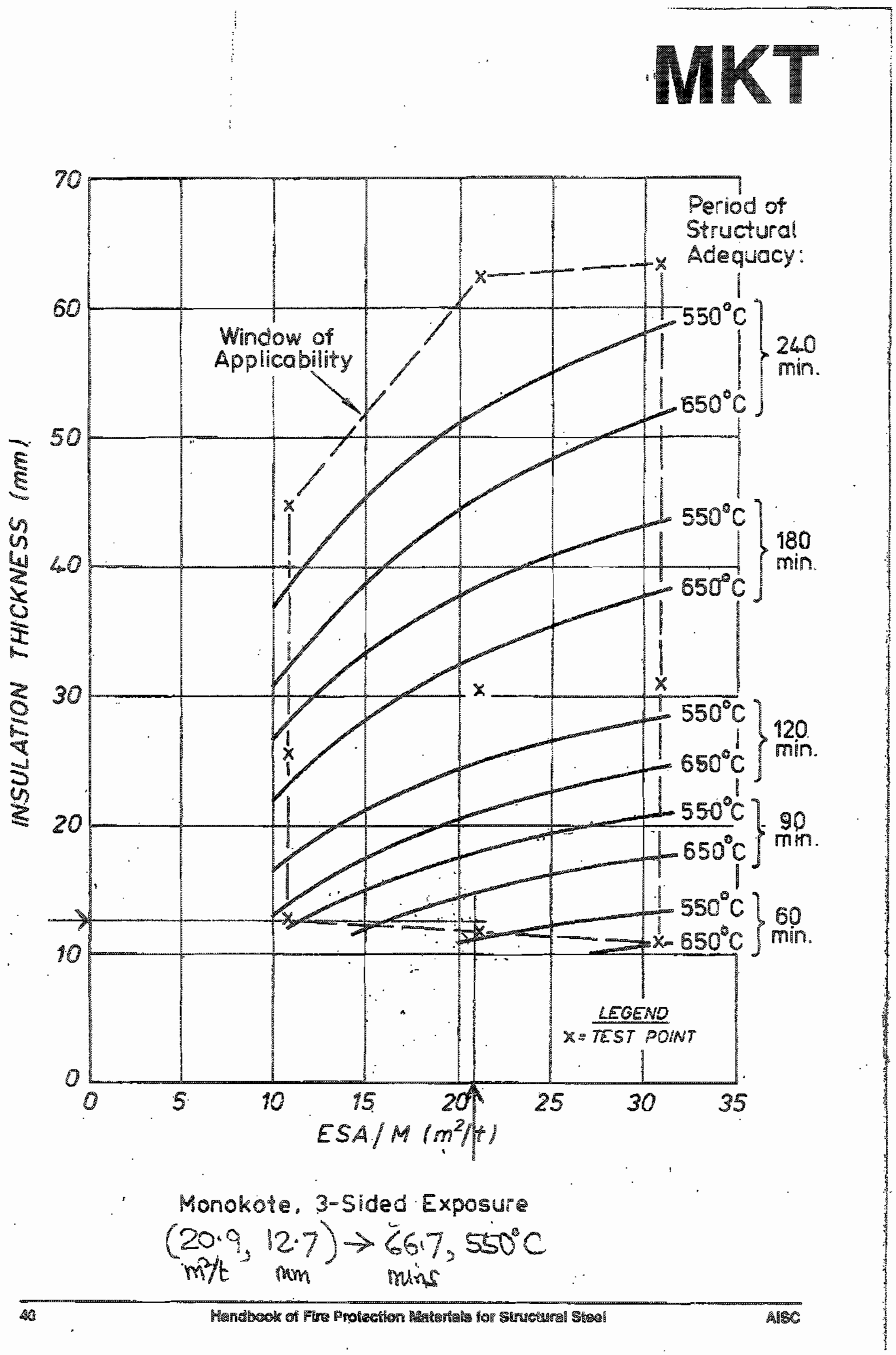
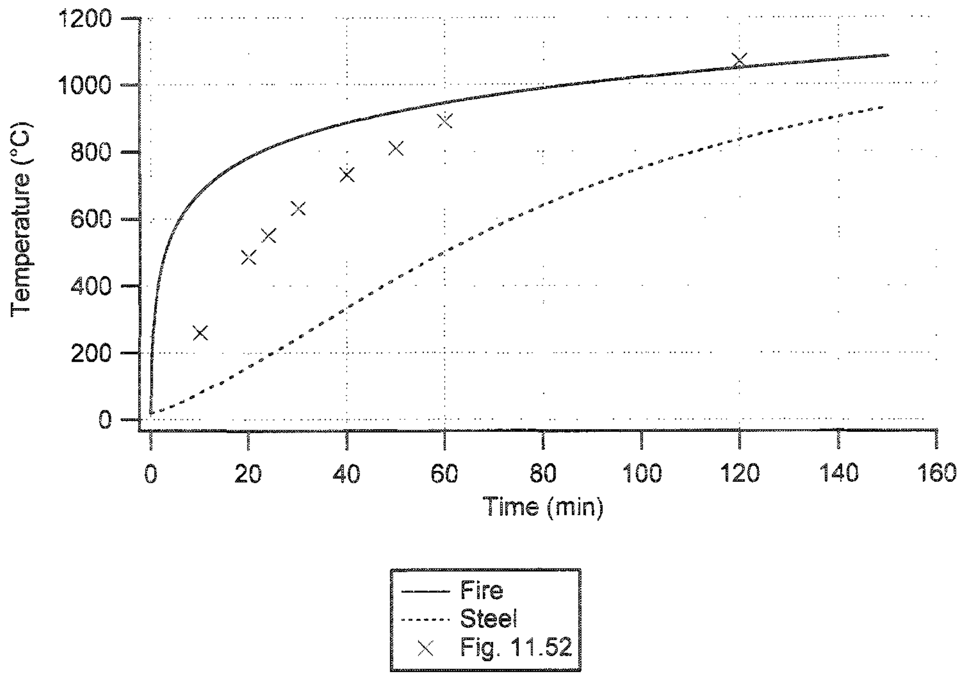


FIGURE A1 : EXTRACT FROM HANDBOOK OF FIRE PROTECTION MATERIALS

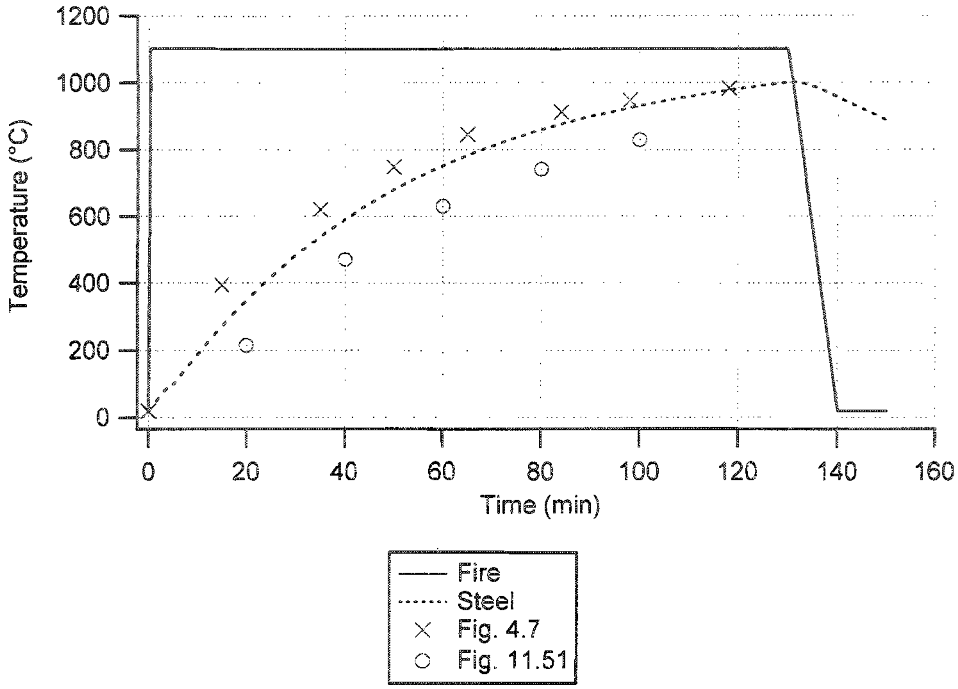


Fire : STTC

Insulation-protected section:
k_{sm} = 20.9 m^2/t
c_s = temperature-dependent
h_i = 12.7 mm
ρ_i = 300 kg/m^3
c_i = 1100 J/kg-°C
$T_i = (0.5T_f + 0.5T_s)$
k_i = 0.04 at 20°C, 0.079 at 500°C, 0.14 at 800°C (W/m-°C)
p_i = 3% by mass
Ref: SFPE Handbook, 2nd Ed., p. 4-188

Result:
T = 550°C, t = 66.6 mins
T = 931°C, t = 150.0 mins (peak)

FIGURE A2 : STANDARD FIRE EXPOSURE



Fire : Linear

Insulation-protected section:
k_{sm} = 20.9 m^2/t
c_s = temperature-dependent
h_i = 12.7 mm
ρ_i = 300 kg/m^3
c_i = 1100 J/kg-°C
$T_i = (0.5T_f + 0.5T_s)$
k_i = 0.04 at 20°C, 0.079 at 500°C, 0.14 at 800°C (W/m-°C)
p_i = 3% by mass
Ref: SFPE Handbook, 2nd Ed., p. 4-188

Result:
T = 550°C, t = 36.0 mins
T = 1001°C, t = 130.8 mins (peak)

FIGURE A3 : EXPOSURE TO 1100°C FIRE