APPLICATION OF THE REVISED REQUIREMENTS OF IAEA ST-1 TO THE TRANSPORT OF NATURAL URANIUM HEXAFLUORIDE.

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SUMMARY

The 1996 issue of IAEA ST1 (formerly SS6) will, from January 2001, call for transport cylinders containing natural uranium hexafluoride ('hex') to be considered against the requirements of the IAEA thermal test. In the case of the 48inch diameter cylinders normally used for commercial transport of natural hex, a package design will require -

- from January 2001, Multilateral Approval if compliance with the thermal test has not been proven. - or
- from January 2004, Unilateral Approval if it has been shown to satisfy the requirements of the thermal test.

In parallel with the revision of SS6, the IAEA established a Coordinated Research Programme (CRP) to consider the behaviour of hex cylinders in fires. BNFL has contributed to that CRP principally in the areas of computer modelling of the thermophysical processes occurring within the cylinder and of the mechanical behaviour of the cylinder itself. Further details of the early work are included in References [Bailey, 1995; Clayton, 1991; Lomas, 1992]. Validation of these models, and those of other contributors to the CRP, has been provided by reference to past experimentation, and to the extensive 'Tenerife' test programme carried out as a French/Japanese/EEC joint contribution to the CRP.

Confidence in the various computer simulations has grown steadily as the CRP progressed. Despite this, the predicted survival time remained uncertain to the extent that it could still be either a little longer or a little shorter than the 30 minutes specified by the IAEA standard. With the benefit of results emerging from the Tenerife test programme, our calculations now predict that an unprotected, fully loaded, 48Y hex cylinder would be expected to survive the thermal test. However, the limited experimental data, and the small predicted margin of safety, mean that survival cannot be guaranteed.

Since the expectation is that the unprotected fully loaded 48Y cylinders should (marginally) survive the fire test, and also taking account both of the excellent safety record to date of hex transports, and the rarity of intense long-lived fires in transport situations, there is clearly very little safety benefit to be gained from the use of fire protection covers on natural hex cylinders. However, the physical handling of such covers, necessarily large and cumbersome, and their relevant fastenings, particularly during packaging, loading, unloading and unpackaging in transport depots would, for every cylinder movement, impose additional risks of injury to transport system operators, which we believe could outweigh the small and more hypothetical benefit of improved fire resistance while in transit. We therefore maintain that it

would be counterproductive, in both overall safety and in cost terms, to require protective coverings on these cylinders for transport.

Based on the estimates made to date, we believe that the unprotected 48inch cylinders should continue to be the normal Package in commercial use, under the relevant Multilateral Approvals.

MODELLING OF HEX BEHAVIOUR WITHIN THE CYLINDER

Lumped Parameter Model

The lumped parameter model was constructed with the intention of incorporating all the possible heat transfer phenomena that could occur during the transient heating of a hex container. Radiant and convective heat from the fire is transferred to the solid hex by conduction through the container wall, by radiation and convection across the ullage and, initially, across the narrow vapour gap, assumed to be present at the initiation of the transient. During this initial stage the wall temperature becomes so high that, when liquid forms, film boiling occurs, rather than nucleate boiling. The film boiling mechanism is then maintained throughout the heating process, because of the high wall temperatures. The evaporation / condensation cycle operates while the liquid level rises in the annular space between the solid and the wall and ceases when the liquid submerges the solid. Radiation and convection continually transmit heat across the ullage space. The container pressure is assumed to be the vapour pressure corresponding to the bulk liquid temperature. Using the model initialised for a type 48Y cylinder, Figure 1 depicts the response of the container and contents.The "step" on the curve begins when melting starts, and rapid temperature rise of the liquid hex in the gap follows, with an associated rapid rise in vapour pressure. The "step" finishes when the liquid in the gap floods over the solid hex, which is then better able to act as a heat sink for the heated liquid.

Finite element model

This second model is a two dimensional finite element simulation using an adaptive meshing code. Due to the transient nature of the problem, there is clearly scope for temperature variations within the individual phases of hex that the lumped parameter model cannot resolve. To investigate the extent of temperature variations within the container and to enable a more precise prediction of the temperature profile around the container, a finite element model of a horizontal cylinder was produced. The various phase change processes were incorporated as additional FORTRAN subroutines into an existing finite element, conduction heat transfer code, the code having been developed primarily for the analysis of phase change problems by the University College of Swansea. The Swansea code uses an adaptive remesh technique, so that mesh refinement in the phase change region can ensure that errors associated with enthalpy changes are minimised. A typical remesh is shown in Figure 2.

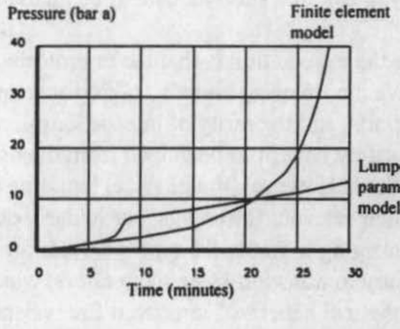


Figure 1. Comparative pressure predictions for early BNFL models

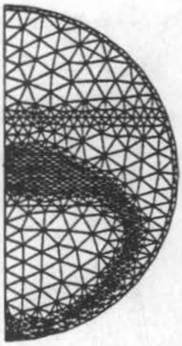


Figure 2. Typical adapted mesh for finite element model

The thermal response predicted by the finite element program is at first similar to that predicted by the lumped parameter model. Initially, heat input from the fire causes the solid to sublime with liquid formed at a later stage in the transient. However, sublimation is predicted to terminate much sooner than in the lumped parameter simulation so that the container wall temperature does not rise above the critical value before liquid is formed. Consequently, the liquid wets the wall and nucleate boiling heat transfer is predicted. The pressure transient resulting from this model is shown in Figure 1 and it can be seen that, although the pressure predictions from the two models are almost numerically equal at 20 minutes into the transient, the finite element model predicts an almost exponential rise after that time whereas the lumped parameter model shows only a slight increase in the rate of pressure rise.

CYLINDER STRENGTH ASSESSMENT

Plastic strain analysis, taking account of thermal and pressure stresses, has been carried out for the UF_6 cylinder. This finite element computation included heat transfer, thermal stress, and mechanical stress analyses to predict the temperatures, stresses, displacements and strains that could be induced into the structure of the container after 30 minutes exposure to an 800°C fire. Previous work [Bailey, 1995] was restricted to calculations based on 'engineering stress'; the present studies extend this by using 'true stress' for calculation of plastic deformations.

Based on bulk conditions estimated to occur near the end of the standard 30 minute fire test, three dimensional finite element thermal analysis using ANSYS generated the temperature profile for use in the mechanical analysis. Obviously those parts of the cylinder in contact with liquid hex are much cooler than other regions. A large temperature gradient occurs in the shell in the region of the liquid/vapour interface. The uppermost areas of the shell reach temperatures of about 660°C, while the areas below liquid level are generally below 300°C.

The above temperature profile was taken as input to the strain modelling. The effects are evaluated in terms of Von Mises equivalent plastic strain. The key steel properties used are for SA-516 Grade 55, using information from [Lunt, 1991] and from tests commissioned specifically for this project. From this latter work, we have also measured the degree of

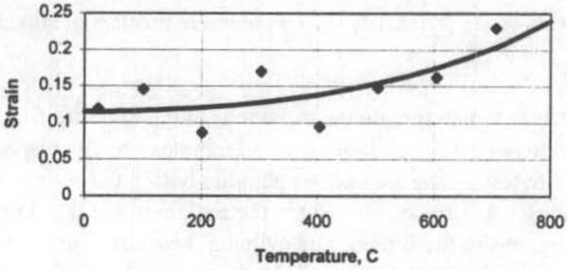


Figure 3. Limits of uniform plastic strain

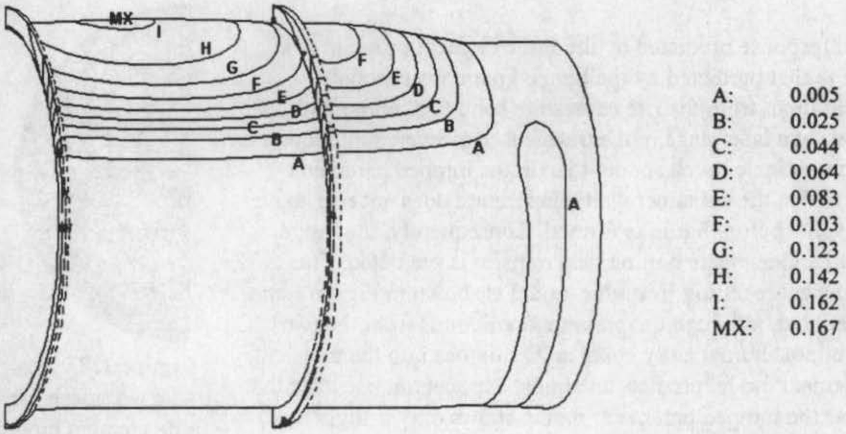


Figure 4. Plastic strain contours at 42 bar(g) (1/4 cylinder shown)

uniform thinning which occured on the tensile test specimens, in order to estimate the hardening modulus and the allowable plastic strain for calculations of burst pressure.

Figure 3 shows our experimental results for maximum uniform plastic strain, prior to the onset of necking (ie at the UTS point) in the tensile tests at various temperatures. There is considerable scatter in these single results, but it can be seen that, in the critical upper regions of the cylinder shell, where temperature is in the region of 660°C, an equivalent plastic strain of ~17% to 20% appears to be the maximum allowable before failure.

The finite element strain modeling was undertaken in two stages. First the plastic strains due to the temperature effects alone were evaluated. Differential thermal expansions resulted in a maximum figure of 1.6%, occurring on the outer edge of the central stiffening ring. Plastic strains of up to about 0.5% occurred in the shell itself, in the areas just above the liquid. The pressure was then gradually increased . Figure 4 shows the strains occurring when the internal pressure reaches 42 bar(g). The maximum equivalent plastic strain is 17%. By comparison with the above estimated allowable strain of 17% to 20%, the burst pressure should therefore be in the region of 42 bar(g).

INTERPRETATION OF TENERIFE RESULTS

We are indebted to the French, Japanese, and European Union sponsors for the main results of the full Tenerife tests series [Niel, 1997]. The pressure profiles obtained are reproduced here on Figure 5.

In this section we attempt to extrapolate the behaviour of the Tenerife containers over 18 - 24 minutes to the behaviour of a 48Y cylinder over 30 minutes. In this respect it important to note that in the Tenerife tests, pressure and temperature within the cylinder continued to rise after the furnace heating was stopped, due to the thermal inertia of the furnace. Particularly in the critical upper regions of the furnace and cylinder, heat continues to be radiated into the vapour space and onto the liquid surface, until the furnace cools. Following the criteria of the standard IAEA test, at the end of the 30 minute fire exposure period the cylinder would be immediately exposed to an ambient temperature of 38C. In these circumstances, the maximum temperature of the steel of the cylinder would rapidly fall, reducing the vapour pressure in the cylinder and increasing the shell's resistance to rupture. It is therefore clear that survival of a cylinder to the end of the 30 minute fire exposure period does represent passing the IAEA fire test: there is insufficient thermal inertia in the cylinder itself to cause failure after that point.

The first stage in the extrapolation is to consider the relative time constants of the two cylinders. Heat input rates will be principally proportional to the surface area of the cylinder, and for radiation dominated heat transfer the skirt and stiffening rings will have little effect. For similar geometry, etc, the time taken to respond to a given heat input will be, to a first approximation, proportional to the mass of hex in the cylinder. We therefore take the 'time constant' of the cylinder in a fire situation as being proportional to its design hex content divided by its (vessel) surface area. From this we can deduce times for the Tenerife containers which are equivalent to 30 minutes for the 48Y and 48X cylinders. These times are 24.6 and 25.7 minutes respectively. It is therefore necessary to consider extrapolation of the Tenerife cylinder's behaviour to approximately 25 minutes.

It might also be expected that the 'time constant' of a test cylinder would be inversely proportional to the incident heat flux. However, over the ranges of interest in these comparisons, the empirical effect of heat flux upon the internal heat transfer mechanisms appears to cancel out the effect of the magnitude of the heat flux, in terms of the resultant rates of pressure rise. This is shown by the empirical data of tests TEN4 and TEN6; despite having very different incident heat fluxes, the pressure rise times are overall very similar. The explanation is not established, but might lie in different boiling heat transfer regimes, with consequent variations in the proportions of heat going into the different phases of hex present in the cylinder.

The relevance of input heat transfer area, however, is well substantiated by the TEN5 experiment. Blanking off some 50% of the surface area gave a pressure rise over 24 minutes consistent with, or indeed somewhat less than, the rises which occurred in TEN4 and TEN6 in 12 minutes.

Due to deviations expected as a result of previous heating of the test cylinder [Niel, 1997], the results of TEN2 must be regarded as less representative in absolute terms than the results of

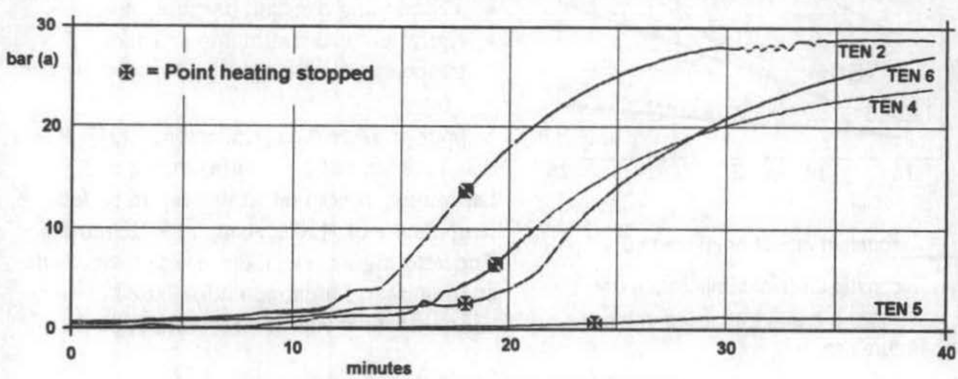


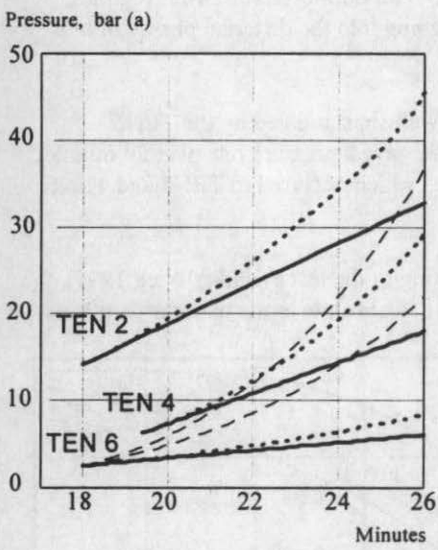
Figure 5. Pressure traces from Tenerife tests

the other tests. Quantitative interpretation should therefore be largely based on tests TEN4,5, & 6. However, the TEN2 test gives valuable qualitative information on the processes occuring, particularly since stratification of the liquid progressed furthest in this test. Our extrapolation method is therefore described by reference to TEN2, starting from the 18minute point where furnace heating was stopped.

In order to extrapolate the pressure data, one option would be to assume a constant rate of rise of pressure from this point forward. Although the pressure record suggests that the rate of pressure rise was very slightly falling at the 18 minute point, it would be difficult to fundamentally justify an extrapolation based on the rate of rise in pressure not increasing. This uncertainty is due to the 'exponential' dependency of vapour pressure upon temperature.

From the detailed results of TEN2, and review of the underlying mechanisms such as discussed in Reference 6, it is clear that the various heat transfer and hydrodynamic processes had stabilised in the final few minutes of heating. The dominant mechanism for pressure rise had become radiant heating of the liquid surface from the upper, very hot, areas of the vessel. The liquid surface had become stably stratified due to temperature gradient effects, the bulk liquid agitation rates being no longer sufficient to overcome the stratification. The vapour space pressure, being a direct function of the liquid surface temperature, was therefore rising quite rapidly.

A conservative estimate of the pressure rise which would have resulted from continued heating can be obtained from consideration of the above mechanism. The controlling influence is principally the temperature of the surface of the liquid hex within the cylinder, since the vapour space pressure must equate to the hex saturated vapour pressure at that temperature. Since relevant heat transfer driving forces are all at this stage reducing, it is clear that an assumption of constant rise rate in the liquid surface temperature should lead to an over-estimate of the resulting cylinder pressure.



______constant rate of pressure rise

........constant rate of temperature rise

_ _ _ _ TEN 6 - with Enhanced rates of temperature rise

Figure 6. Extrapolations of pressure rises

Hence we may reasonably assume that the pressure profile will be intermediate between;-

- a continuing constant rise rate, and
- equivalent to a continuing constant temperature rise rate.

Vapour pressure data [Anderson, 1994], shows that a pressure of 14.1 bar(a), rising at 2.35 bar/minute, is equivalent to a liquid surface temperature of 430K, rising at 9.1K/minute. The latter figures can therefore be taken as the starting point for extrapolation based on a constant rate of rise of temperature, $d\theta/dt$.

From the pressure traces of TEN4 and TEN6, it is immediately apparent that the average pressures arising are much less severe, both in terms of speed of arising and in terms of magnitude, than were the pressures arising in the TEN2 test. Following this same methodology, Figure 6 shows the results of extrapolating the three tests.

In the case of the TEN4 test, stable stratified conditions appear to have become established by the end of the heating period, so that the extrapolations are believed reasonable.

In the case of the TEN6 test, there was so little pressure rise before turning off the furnace that stable stratified conditions had yet to be established, so that extrapolation on the same basis as the preceeding tests may not be appropriate. In order to find a definitely pessimistic extrapolation, suppose that conditions were just about to become stratified at the moment of heating stop. Also take the higher of the two previous temperature rise rates as a basis - ie 12.7 K/min as found for test TEN4. Further, because of the increased furnace temperature used for this test, increase the surface temperature rise rate in ratio to the expected gross increased radiation rate, ie multiply by ((880+273)/(800+273))^4. This gives a very worst case temperature rise rate of 16.9 K/min. The results of applying these enhanced temperature rise rates are also shown on Figure 6.

	Conditions at heating stop					
	Time	Pressure bar(a)	Pressure rise rate bar/min	Equivalent temperature °K	Equivalent temperature rise rate, °K/min	Pressure @ 25 min, predicted from temp. rise rate. bar(a)
TEN2	18m	14.1	2.35	430	9.1	39
TEN4	19m 23s	6.2	1.78	390	12.7	24
TEN6	18m	2.5	0.44	354	6.2 (12.7) (16.9)	8 (18) (29)

The above table summarises the extrapolation results. From this data, noting that TEN2 is not considered quantitatively as reliable as the other tests, it would be reasonable to expect the true value for the pressure reached within a cylinder at the end of the fire test to be within the range 20 to 30 bar(a).

CONCLUSIONS

Our calculations on cylinder strength, and our estimates of pressures likely to arise during fire exposure, based on our interpretation of the completed series of 'Tenerife' tests in combination with our previous calculations, indicate that unprotected, fully loaded, 48Y cylinders would be expected to survive (marginally) the IAEA fire test. Therefore, taking account of the excellent safety record to date of hex transports, and the rarity of intense long-lived fires in transport situations, there is clearly very little safety benefit to be gained from the use of fire protection covers on natural hex cylinders. However, the physical handling of such covers, necessarily large and cumbersome, and their relevant fastenings, particularly during packaging, loading, unloading and unpackaging in transport depots would, for every cylinder movement, impose additional risks of injury to transport system operators, which we believe could outweigh the small and more hypothetical benefit of improved fire resistance while in transit. We therefore maintain that it would be counterproductive, in both overall safety and in cost terms, to require protective coverings on these cylinders for transport.

The fact that we cannot at this stage be absolutely sure that the unprotected cylinders satisfy the fire test means that Multilateral approval may in due course be required by the new provisions of IAEA ST-1. Based on the estimates made above, we believe that unprotected 48Y cylinders remain an appropriate means of transport for natural UF_6 .

ACKNOWLEDGEMENT

We wish to acknowledge the major advance in available empirical data on the behaviour of hex under fire exposure conditions, made by the 'Tenerife' experimental programme, carried out as a French/Japanese/EEC joint contribution to the IAEA CRP. This work has verified the principles of the various mathematical models, and allows more accurate quantification of their relevant parameters. Also, being close to full scale tests, the data can be extrapolated to full scale with much greater confidence than previous smaller scale data.

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