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Modelling of vented gas explosions

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Summary

The commonly used empirical correlations for the dimensioning of low-strength and high-strength enclosures are reviewed. Empirical guidelines, however, have limited accuracy in their accuracy to extrapolate to full-scale model.

Generalised mathematical models for vented explosions consist of a system of three first degree differential equations for dimensionless variables that can be solved numerically. The most recent one of these by Molkov has been validated with data of vented hydrocarbon-air and hydrogen-air explosions. Based on the calculations with this model, Molkov has proposed a so-called universal correlation for reduced pressure of vented explosion.

Accidental indoor spills of volatile solvents and the release of flammable gases within buildings and equipment can form stratified (to the floor) on layered (to the ceiling) flammable vapour or gas mixtures. If these mixtures are ignited, partial volume deflagrations may occur.

Orlando presented a method for vent dimensioning in enclosures partially filled with stratified or layered gas-air mixtures. The method modifies slightly the experimental correlations by Bradley and Mitcheson for low-strength and by Bartknecht for high-strength enclosures. This method has been validated with test data from vented explosions of partially filled enclosures and found to give moderately conservative predictions for almost all the tests.

In this report, the method of Orlando is applied to tests where a part of the enclosure has been filled with homogenous mixture. The method gives in most cases non-conservative predictions for such tests.

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1 Introduction

A previous literature study (Lautkaski 1997) discussed physical phenomena involved in vented gas explosions and methods in use to predict the peak pressures from parameters describing the enclosure and burning velocity of the gas-air mixture. The methods to predict pressure rise in confined gas explosions were reviewed and applied to hydrogen explosions in the report (Lautkaski 2005). The aim of the present study is to review various models developed for the simulation of the course of vented gas explosions and methods to predict the peak pressures in enclosures partially filled with flammable mixtures.

2 Basics of vented gas explosions

A basic quantity of premixed gas flames is the laminar burning velocity S_0 [m/s]. This is the velocity at which a planar flame front (thin reaction zone) travels in a laminar flow with respect to the unburned mixture immediately ahead of it. The burning velocity is usually measured in a test apparatus in which the flow velocity of the mixture is adjusted so that the flame front is stationary. The Bunsen burner burning premixed gas has been used to measure burning velocities (Figure 1).

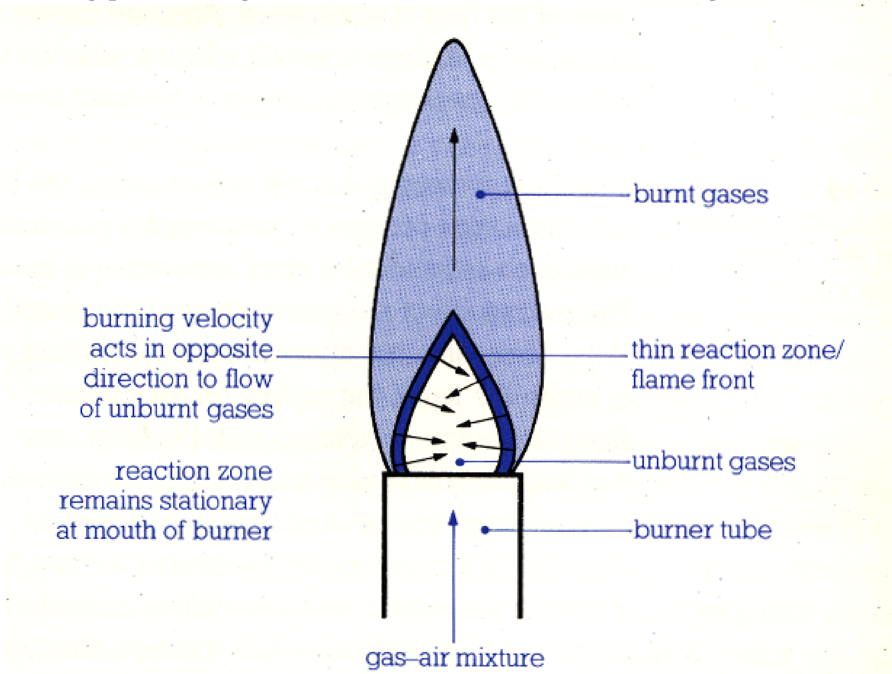

Figure 1. Stationary premixed flame of a Bunsen burner (Harris 1983).

The value of the laminar burning velocity is determined by molecular transport processes, such a heat and mass transfer within the flame front. The burning velocity is a function of gas concentration, reaching a maximum just on the fuel rich side of the stoichiometric concentration. This maximum value, sometimes called the fundamental burning velocity, is tabulated for several gases e.g. in NFPA 68.

For a given fuel concentration, the laminar burning velocity S_0 is dependent on both temperature and pressure. For the purpose of engineering studies, the dependence is usually taken to be

$$S_0 = S_r \left(\frac{T_0}{T_r} \right)^{\alpha} \left(\frac{p}{p_0} \right)^{\beta} \tag{1}$$

where p_0 [bar] and T_0 [K] are the initial pressure and temperature, p [bar] is the pressure and T_r [K] is the temperature at which the reference value of burning velocity S_r [m/s] has been measured. The exponent α is usually set equal to 2. The exponent β is substance specific (Metghalci & Keck 1982). Actually, the pressure dependence of S_0 is quite weak since the value of β is about 0.25 for hydrocarbons (Shepherd et al. 1997) and about 0.2 for lean hydrogen-air mixtures (Gelfand 2000).

In a gas explosion, the situation is different. The flame front is travelling away from the ignition point in a moving gas-air mixture. The expansion of combustion products acts as a piston pushing the unburned mixture away from the point of ignition. It is helpful to think the piston as a porous one, permitting the unburned mixture to flow through. The velocity of the flame front with respect to some fixed position is the sum of the flow and burning velocities. This velocity is called the flame speed v_f [m/s].

Assuming that the gas mixture is initially at rest, the flow is laminar, the flame surface is smooth and the burned gases are at all times trapped behind the expanding flame front, the relationship between the flame speed and burning velocity can be expressed as (Harris 1983)

$$v_f = ES_0 \tag{2}$$

The expansion factor E is the ratio of final and initial volume of the mixture at constant pressure

$$E = \frac{N_f}{N_i} \frac{T_f}{T_i} \tag{3}$$

where N_f and N_i are the final and initial number of moles and T_f and T_i are the final and initial temperature [K] of the mixture. When adiabatic combustion can be assumed the final temperature is equal to the adiabatic flame temperature T_{ad} [K].

The assumption of burned gases trapping is valid for several geometries e.g.:

— in a pipe closed at one end and ignited at that end
— in a tank ignited at centre.

Figure 2 presents the effect of flame geometry on the flame speed in two idealised cases. A smooth flame with a constant area propagates in a tube with a circular cross-section towards the open end. The flame front is assumed to be either planar (Fig. 2A) or hemispherical (Fig. 2B). In the former case, $v_f = ES_0$ and in the latter case, $v_f = 2ES_0$.

In reality, when a flame front propagates in any geometry, it can develop a cellular structure showing peaks and troughs, often collectively called wrinkles. The volume production of burned gases, which expand to drive the flame front forward, is proportional to the actual surface area of the flame. This effect can be considered by adding an area correction to Eq. (2) (Harris 1983)

$$v_f = E \frac{A_f}{A_n} S_0 \tag{4}$$

where A_f and A_n are the actual and idealised (laminar) flame areas [m^2].

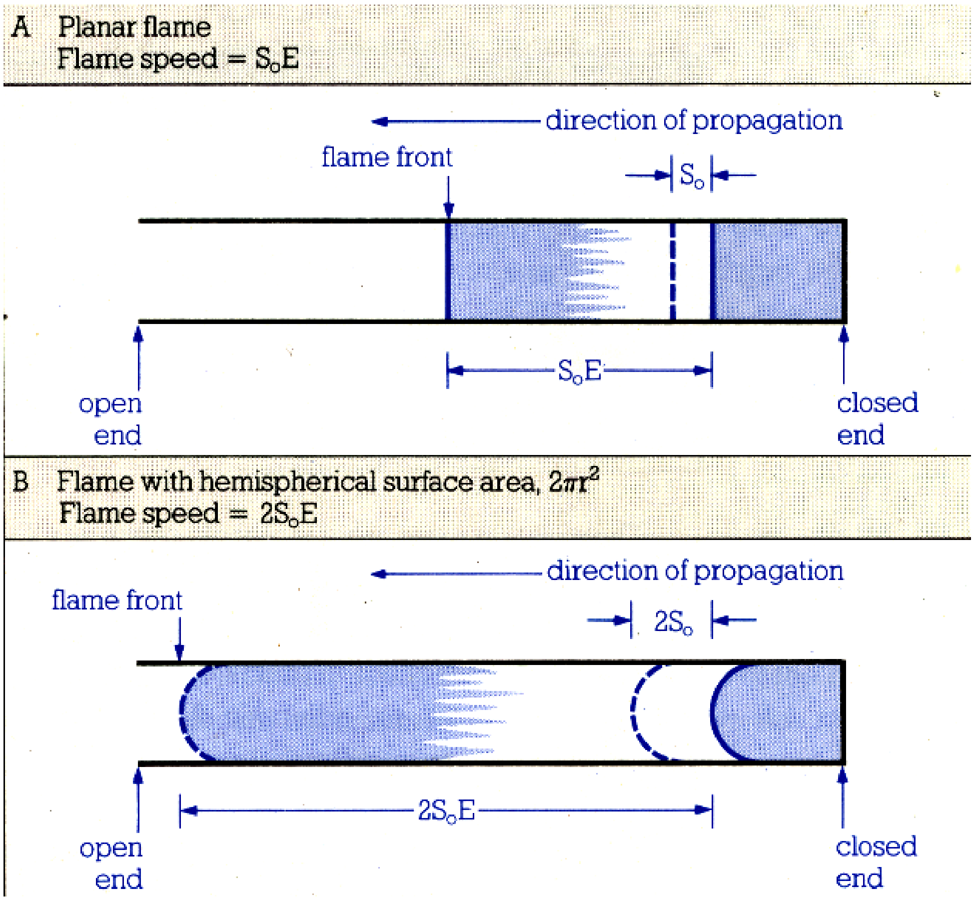

Figure 2. Effect of flame area on flame speed. A. Planar flame. B. Flame with a hemispherical surface area (Harris 1983).

Unfortunately, there is no simple method to predict the actual flame area A_f. It is to be stressed that the burning velocity is a fundamental property of any gas-air mixture, the flame speed is not. The flame speed is a useful concept and the laminar flame speed is a lower limit to the real (turbulent) flame speed.

In gas explosions, there are other effects which may increase the flame speed even considerably. The most important one is turbulence which can be generated by factors such as
— wall friction (especially effective in pipe explosions)
— high flow velocities e.g. near an explosion vent
— obstacles throttling the flow and generating vortices in their wakes.

The flame speed of a front propagating in a turbulent flow is affected by the turbulence in two ways:
— the large turbulent eddies increase the flame area
— the small turbulent eddies increase the diffusion of heat and mass.

Both effects increase the flame speed v_f; the large eddies by increasing the area ratio A_f/A_n and the small ones by increasing the burning velocity from the laminar one to the turbulent burning velocity S_f.

When a flammable mixture fills a cubical enclosure and is ignited at the centre, flame front remains spherical until it touches the walls. Consequently, the flame speed is only moderately accelerated and reaches a final value less than about 10 m/s.

In an elongated enclosure with the length to diameter ratio L/D less than 5, spherical propagation of the flame takes place only in the initial stage of the explosion. Subsequently, the flame front will proceed swiftly in an axial direction where it will contact a precompressed flammable mixture. This will cause the violence of the explosion to increase and oscillations are superimposed on the course of explosion. For elongated vessels with L/D > 5, even transition of deflagration to detonation can occur (Bartknecht 1981).

3 Venting guidelines

The basic problem of explosion venting of an enclosure of volume V [m^3] filled with a flammable gas-air mixture is to select the vent area A_v [m^2] so that the explosion overpressure does not exceed a maximum permissible value of P_{red} [kPa]. The first systematic tests were performed by Cubbage and co-workers in the 1950s. The test data was used to derive experimental correlations that could be used to select A_v. Alternatively, the correlations could be used to predict P_{red} for given values of V and A_v. These parameters are combined into a dimensionless parameter K called vent coefficient and defined as

$$K = \frac{V^{2/3}}{A_v} \tag{5}$$

Tests by Cubbage and Simmonds (1955, 1957) were performed in chambers of 0.2, 1.5, 2.8 and 14 m^3 volume using mainly town gas-air mixtures, although some experiments were performed with other gases and vapours. The explosion relief panels were restrained either by gravity or a minimum amount of friction. Consequently, the resulting venting guideline is strictly applicable only to situations in which the vent opening pressure P_{stat} [kPa] does not exceed about 2 kPa.

In these tests, two successive pressure peaks were recorded. The creation of the first peak P_1 can be described as follows: Before the vent opens, the pressure increase is caused by the production of hot combustion products generated by the flame front travelling at the flame speed v_f. The rate of volume generation dV/dt (here V is the volume of gas mixture at initial pressure [m^3]) is the difference of hot combustion products appearing and unburned mixture disappearing (Bradley & Mitcheson 1978a)

$$\frac{dV}{dt} = 4\pi r_f^2 v_f E - 4\pi r_f^2 v_f = 4\pi r_f^2 v_f (E-1) \tag{6}$$

Pressure in the room is equalised by compression waves travelling at sound velocity and reflecting from the walls of the room. Thus, at any moment the internal overpressure P will be the same throughout the room.

When the vent is fully open, the flow of gases can be calculated from the formula of incompressible flow (Harris 1983)

$$\frac{dV}{dt} = C_d A_v \sqrt{\frac{2\Delta p}{\rho}} \tag{7}$$

where C_d is the discharge coefficient, Δp [bar] is the pressure difference over the vent and ρ [kg/m^3] is the gas density.

If P_{stat} is low, the flame radius r_f [m] and, consequently, the rate of volume generation Eq. (6) are small. If A_v is large enough, the outflow rate Eq. (7) will be larger than the rate of volume generation Eq. (6). The gas volume in the room will decrease as will the pressure. In this way, the first pressure peak P_1 is generated.

If the vent opens early, the flame radius r_f keeps increasing and the rate of volume generation in Eq. (6) becomes soon larger than the outflow rate Eq. (7). Then the internal pressure P rises until hot combustion products start to flow out of the vent. Their density is the density of the unburned mixture divided by the expansion factor E. Consequently, the outflow rate Eq. (7) is suddenly increased by the factor $E^{1/2}$. The outflow rate Eq. (7) becomes again larger than the rate of volume generation Eq. (6), resulting in the second pressure peak P_2.

According to Cubbage and Simmonds (1955, 1957), the overpressure of the first pressure peak P_1 in kPa can be predicted by the correlation

$$P_1 = \frac{S_0(0.43Kw + 2.8)}{V^{1/3}} \qquad (8)$$

and the second pressure peak P_2 in kPa by

$$P_2 = 5.8 S_0 K \qquad (9)$$

where S_0 [m/s] is the laminar burning velocity of the gas-air mixture and w [kg/m^2] is the mass per unit area of the vent cover.

Eqs. (8) and (9) have been used successfully to predict P_1 and P_2 in volumes up to 200 m^3, under non-turbulent conditions. They can be applied to empty rooms with the maximum to minimum dimension ratio less than 3. The rooms must have relatively smooth internal surfaces and the mixture must be initially quiescent. The vent coefficient K must be less than 5 and the mass per unit area of the vent cover w should not exceed 24 kg/m^2 (British Gas 1990).

Cubbage and Marshall performed tests in chambers of volumes up to 30 m^3, using a variety of fuel gases to maximise the range of S_0. The explosion relief vent was fixed and had to be broken by internal pressure to create open vent. Consequently, the resulting venting guideline is strictly applicable only to situations in which the P_{stat} is larger than about 2 kPa. P_1 is given in kPa by the formula

$$P_1 = P_{stat} + \frac{2.3 S_0^2 K w}{V^{1/3}} \qquad (10)$$

Under these conditions, Eq. (10) has been used successfully to predict P_1 and P_2 in volumes up to 200 m^3. The fact that Eq. (10) is proportional to S_0^2, and not to S_0, leads to some overestimation of P_1 for mixtures with $S_0 > 0.5$ m/s. On the basis of such experiments, British Gas (1990) recommends that the coefficient in Eq. (10) should be reduced to 0.7 when $S_0 > 0.5$ m/s.

Later tests revealed that the time dependence of overpressure resulting from vented gas explosion in a room can be described in terms of four distinct peaks which can (but do not have to occur). The four peaks are (British Gas 1990, Gardner & Hulme 1995):

P_1 which is associated with the pressure drop following the removal of the explosion relief vent and subsequent venting of unburned gas.

P_2 which is associated either with the pressure pulse following the venting of burned gas, or caused by a possible external explosion due to ignition of previously vented unburned gas by the flame emerging from the vent.

P_3 a long duration but generally small amplitude peak associated with the maximum rate of combustion within the room (this typically occurs when the flame front reaches the walls).

P_4 which is an oscillatory pressure peak attributed to excitation of acoustic resonances in the gaseous combustion products. The resulting high combustion rate may cause a significant net overpressure to be developed in the room.

Catlin et al. (1993) have split the second peak into two successive, partly overlapping peaks. The first or P_v follows the venting of burned gas and the second or P_2 results from the external explosion.

Normally, P_3 will not be the dominant peak in a vented explosion, and will be considerable smaller than P_1. Obstacles in the room prevent the formation of the standing acoustic wave necessary for the generation of P_4. The latter can also be prevented by covering the walls with a sound absorbing lining (British Gas 1990).

The U.S. standard NFPA 68 (2007 Edition) uses a modified version of the Runes' formula. The original Runes' formula was based on the assumption that the maximum pressure developed in a vented explosion occurs when the rate of volume generation Eq. (6) and the outflow rate Eq. (7) are equal. The volume generation rate Eq. (6) is taken to have its maximum value which is assumed to occur at maximum flame area i.e. just before the flame is quenched by contact with the walls. On this basis, Runes (1972) presents an equation relating A_v and P_{red}

$$A_v = \frac{CL_1L_2}{P_{red}^{1/2}} \tag{11}$$

where L_1 and L_2 [m] are the two largest dimensions of the room. In effect, the ratio L_1L_2/A_v is the vent coefficient K. Thus, Eq. (11) can also be expressed as

$$P_{red} = C^2K^2 \tag{12}$$

The derivation of Eq. (12) actually leads to an equation for the prediction of P_3. The method predicts significantly larger vent sizes A_v than are necessary in non-turbulent explosions, even for large volumes V and/or elongated enclosures. In turbulent explosions, Eq. (12) would provide reasonable estimates for P_{red}, if an appropriate value for the parameter C could be defined. However, there is no acceptable way to determine C for turbulent explosions, other than full-scale experiment. For this reason, British Gas (1990) does not recommend the Runes' method.

However, the U.S. standard NFPA 68 (2007 Edition) recommends the use of a modified version of Eq. (11)

$$A_v = \frac{CA_s}{P_{red}^{1/2}} \tag{13}$$

where A_s [m^2] is the internal surface area of the enclosure, P_{red} is the reduced pressure [bar] and C [$bar^{1/2}$] is an experimental constant depending on the value of the laminar burning velocity S_0. An expression relating C and S_0 has been derived from tests and investigations of industrial explosions

$$C = 1.57 \cdot 10^{-1} S_0^2 + 1.57 \cdot 10^{-2} S_0 + 0.0109 \tag{14}$$

Eq. (14) is stated to be valid for $0.08 \leq S_0 \leq 0.6$ m/s and $P_{red} \leq 0.1$ bar.

NFPA 68 divides the vent area dimensioning methods into those for low-strength and those for high-strength enclosures. The methods presented above are meant to be used for low-strength enclosures defined as those capable of withstanding overpressures no larger than 0.1 bar. All buildings are low-strength enclosures. High-strength enclosures are defined as those capable of withstanding overpressures larger than 0.1 bar.

The method for calculating the vent area A_v for gas explosions in a high-strength enclosure included in NFPA 68 and EN 14994 has been derived by Bartknecht (1993). The method is based on an extensive program of explosion tests of flammable gas mixtures in tanks and silos.

Explosion characteristics of a gas-air mixture are described by the maximum rate of pressure rise in a closed vessel multiplied by the cube root of vessel volume.

$$\left(\frac{dP}{dt}\right)_{max} V^{1/3} = K_G \tag{15}$$

This quantity is called deflagration index of gas (NFPA 68) or gas explosion constant (EN 14994). The value of K_G varies depending on test conditions, such as type and amount of ignition energy and volume of test vessel. Bartknecht (1993) measured the value of K_G and the maximum overpressure P_m [bar] in a 5 dm^3 closed test vessel at room temperature using a 10 J spark as ignition source.

The area A_v of a vent opening at static overpressure P_{stat} required to limit the overpressure in a near-cubic enclosure with volume V to the value P_{red} can be calculated with the correlation by Bartknecht (1993)

$$A_v = \left[\frac{0.1265 \log K_G - 0.0567}{P_{red}^{0.5817}} + \frac{0.1754\left(P_{stat} - 0.1\right)}{P_{red}^{0.5722}}\right] V^{2/3} \tag{16}$$

The limitations of validity of Eq. (16) are:

— the length to diameter ratio L/D of the enclosure must not exceed 2
— $P_m \leq 8$ bar
— 50 bar·m/s $\leq K_G \leq$ 550 bar·m/s
— 0.1 bar $\leq P_{stat} \leq$ 0.5 bar
— $P_{red} \leq 2$ bar
— $P_{red} - P_{stat} > 0.05$ bar
— $V \leq 1000$ m^3.

According to Siwek (1996), the use of Eq. (16) can be extended to low-strength enclosures with $P_{red} < 0.1$ bar by simply inserting $P_{stat} = 0.1$ bar i.e. by omitting the second term.

For L/D values from 2 to 5, the vent area calculated from Eq. (16) is increased by

$$\Delta A = \frac{A_v K_G}{750}\left(\frac{L}{D} - 2\right)^2 \tag{17}$$

European standard EN 14994 does not use Eq. (17) for elongated enclosures, but uses instead a modified form of Eq. (10) for the dimensioning of explosion vents in an elongated enclosure vented at each end, either through end vents or side vents close to that end

$$P_{red} = P_{stat} + \frac{2.3 S_0^2 w A_{cs}}{V^{1/3} A_v} \left(\frac{L}{D} \right)^{1/3} \tag{18}$$

where A_{cs} [m^2] is the cross-sectional area of the enclosure, L [m] is the enclosure length and D [m] is the enclosure diameter [m]. Here A_v is the total area of explosion vents.

In addition to Eq. (18), P_{red} must also be calculated from Eq. (19) or Eq. (20) and the larger value found must be used to select A_v

$$P_{red} = 1.5 \frac{x A_{cs}}{D A_v} \quad for\ P_{st} \le 6\,kPa \tag{19}$$

$$P_{red} = 1.5 \frac{x A_{cs}}{D A_v} + 15 \quad for\ P_{st} > 6\,kPa \tag{20}$$

where x [m] is the maximum possible distance that can exist between a potential ignition source and the nearest vent.

Eqs. (18) to (20) are stated to be valid only for $S_0 \le 0.46$ m/s, $V \le 200$ m^3, 0.5 kg/m^2 $\le w <$ 5 kg/m^2, $P_{stat} \le 10$ kPa, $P_{red} \le 100$ kPa and $2 < L/D \le 10$.

4 Modelling vented gas explosions

Existing guidelines for predicting overpressure in vented gas explosions are empirically based, essentially being correlations to experimental data. Their application, therefore, is only strictly valid for situations which are closely similar in scale and geometry to the experiments upon which the method is based. The underlying data, however, typically involve small-scale experiments, and yet it is generally accepted that the peak overpressure and impulse density (time integral of the overpressure) get larger, the larger the scale of the experiment.

The increasing magnitude in overpressure with scale arises from the different scale dependencies of the flow and combustion processes in the explosion whose combined influence upon the overpressure can only be inferred by a more fundamentally based model. Empirical explosion overpressure guidelines will therefore have limited accuracy in their ability to extrapolate to full-scale volumes (Catlin et al. 1993).

4.1 The model of Yao

One of the early explosion models was developed by Yao (1974) as a part of a U.S. Atomic Energy Commission research project to improve explosion safety in the design, construction and operation of gloveboxes. A generalised mathematical model of low-pressure venting developed by Yao and co-workers in 1969 predicted two distinctive pressure peaks. The measured second pressure peaks in most cases, however, could only be calculated by employing a turbulence factor χ to increase burning velocity or flame speed several times.

The model described a spherical vessel with an open vent containing a homogenous gas mixture at atmospheric pressure and ignited at the centre. The reduced pressure was assumed to be no higher than 69 kPa so that vent flow would be subsonic.

Figure 3 is a simplified conceptual diagram of the model of the vented explosion. After the vent opens, turbulence of the outflow and suction effects cause the spherical flame front to distort towards the vent in a pear like shape. This is taken into account by the turbulence factor or flame stretch factor $\chi = A_f/A_n$. Here A_f and A_n are the areas of the pear-shaped flame front and spherical flame front, respectively. Consequently, a mixture of unburned and burned gas may be vented (Canu et al. 1990).

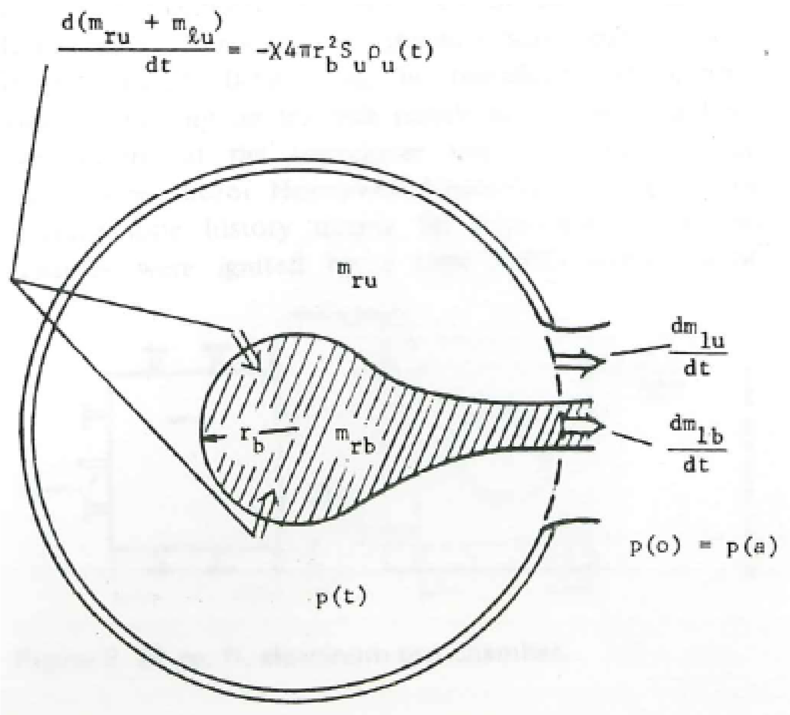

Figure 3. Conceptual model of a vented gas explosion (Yao 1974).

On the basis of mass conservation law and the assumption of adiabatic gas compression (in the approximation of a constant specific heat ratio $\gamma = c_p/c_v$), Yao derived a system of three differential equations for dimensionless rates of change in pressure, burned gas mass and unburned gas mass, respectively. The equations by Yao (1974) as corrected by Anthony (1977/78) are (to facilitate comparison with other models, symbols used by Yao (1974) have been changed to those used by Molkov et al. (1993))

$$\frac{d\pi}{d\tau} = 3\chi\gamma(E-1)E^{2/3}n_b^{2/3}\pi^{1-2/3\gamma}\pi^{2-2/\gamma+\beta} - \alpha\gamma\left[(1-A)E^{1/2} + AE\right]E^{2/3}\pi^{1-1/\gamma}R \quad (21)$$

$$\frac{dn_b}{d\tau} = 3\chi E^{2/3}\pi^{1/3\gamma}n_u^{2/3}\pi^{2-2/\gamma+\beta} - \alpha E^{2/3}AR \quad (22)$$

$$\frac{dn_u}{d\tau} = -3\chi E^{2/3}\pi^{1/3\gamma}n_b^{2/3}\pi^{2-2/\gamma+\beta} - \alpha E^{2/3}E^{1/2}(1-A)R \quad (23)$$

The dimensionless quantities in Eqs. (21) to (23) are:

Dimensionless pressure

$$\pi = \frac{p(t)}{p_0} \tag{24}$$

where p(t) is the absolute pressure and p_0 the initial pressure [bar].

Dimensionless time

$$\tau = \frac{S_0}{a} t \tag{25}$$

where a is the radius of the vessel (Yao defines the reduced time as $E^{2/3}S_0t/a$).

Dimensionless burned gas remaining

$$n_b = \frac{m_b(t)}{m_0} \tag{26}$$

Dimensionless unburned gas remaining

$$n_u = \frac{m_u(t)}{m_0} \tag{27}$$

The venting parameter α is

$$\alpha = \frac{\sqrt{2} C_d A_v a}{S_0 V E^{7/6}} \left(\frac{p_0}{\rho_{u0}} \right)^{1/2} \tag{28}$$

where ρ_{u0} is the density of unburned mixture at the initial pressure p_0 and temperature T_0.

The parameter A is the fraction of vent area through which burned gas flows ($0 \leq A \leq 1$). The parameter R (for subsonic flow) is

$$R = \sqrt{\frac{\gamma}{\gamma - 1} \left(\pi^{1-1/\gamma} - 1 \right)} \tag{29}$$

The first term on the right side of Eqs. (21) to (23) represents the pressure rise, and mass rate change of the burned and unburned gases, respectively, due to flame propagation inside the enclosure. The second term represents the effects of the efflux through the vent opening on these variables (Yao 1974).

Anthony (1977/78) has corrected several printing errors in Yao (1974). The term $\pi^{2/3}$ has been replaced by $n_b^{2/3}$ in Eq. (21) and sign of the first term in Eq. (23) changed. The multiplier $E^{1/2}$ has been added to the second term of Eq. (23) to account for the effect of higher density of the unburned gases on the mass flow in the vent. He has also accounted for the dependence of S_0 on dimensionless pressure π, implicit in the original equations by Yao. The temperature of unburned gas mixture can be predicted using an isentropic relationship

$$T_u = T_0 \left(\frac{p}{p_0} \right)^{1-1/\gamma_u} \tag{30}$$

Insertion of Eq. (30) into Eq. (1) gives

$$S_0 = S_r \left(\frac{p}{p_0} \right)^{\alpha - \alpha/\gamma_u - \beta} = S_r \pi^{\alpha - \alpha/\gamma_u - \beta} = S_r \pi^{\varepsilon} \tag{31}$$

The multiplier of S_r in Eq. (31) with α = 2 and $\gamma_u = \gamma$ has been inserted into the first terms of Eqs. (21) to (23).

Experiments were performed in a 0.765 m^3 cubical chamber and two cylindrical chambers with 0.91 m diameter and a length of 0.91 m or 2.74 m. The chambers were filled with a slightly rich propane-air mixture. The pressure-time records measured in tests with an open vent of area 0.29 m^2 gave the result that the shape and magnitude of the experimental pressure peak could be predicted using the value $\chi = 2$. The time of the peak, however, agreed with a prediction using the value $\chi = 1$. This result suggested that the turbulence factor χ was actually a variable that increased with time as an unknown function.

The model of Yao was used to simulate gas explosion in an enclosure with an initially closed vent opening at a low pressure as follows:

1. After ignition, the flame front is spherical as in a closed vessel explosion. This was modelled by inserting $\alpha = 0$ into Eqs. (21) and (22). The pressure increased up to the vent opening pressure P_{stat}.
2. The flame front area was assumed to increase suddenly by the factor of χ. The value of the parameter A was set equal to the mass ratio of burned gas to total gas mass remaining in the enclosure $A = n_b/(n_b + n_u)$.
3. The flame front area at vent opening time was relatively small. The vent area was large enough to cause a rapid reduction of pressure. The pressure increased to the second peak pressure as the flame approached the wall of the enclosure.

The tests with a covered vent produced pressure-time records with two separate peaks. The value of the first peak was identified as the bursting pressure of the vent cover. Unexpectedly, the magnitude and time of the second peak varied considerably in tests with the cubical chamber with a 0.29 m^2 vent. In one test, the measured second peak occurred at 190 ms and lasted for 105 ms. In a second test, the corresponding times were 270 ms and 80 ms, respectively. The former peak could be predicted reasonably well assuming $\chi = 1.5$ and the latter assuming $\chi = 3.5$.

This again suggested that the turbulent effect was induced gradually during the venting process, and reached its peak level of $\chi = 3.5$ when the maximum relative movements between the burned and unburned gases have occurred. The long delay in the appearance of the second peak was not well understood (Yao 1974).

4.2 The model of Crescitelli et al.

Crescitelli et al. (1980) criticise the model of Yao (1974) being non-practical because the turbulence factor χ is not related to the measurable parameters. They present a system of three differential equations for the pressure ratio π, the dimensionless amount of vented gas $n_v = 1 - n_u - n_b$ and a dimensionless energy variable η which takes into account also gas expansion work inside the enclosure.

The laminar burning velocity S_0 is assumed to vary with pressure according to Eq. (1). A constant specific heat ratio γ is assumed. To calculate the portion of burned gases in the enclosure, a simplified heat balance equation is used (to facilitate comparison with other models, symbols used by Crescitelli et al. (1980) have been changed to those used by Molkov et al. (1993))

$$n_b = \frac{\pi - 1 - n_v + \eta}{\pi_m - 1} \tag{32}$$

The three differential equations of the model for venting of unburned mixture are

$$\frac{d\pi}{d\tau} = 3\chi(\pi_m - 1)\left[1 - \frac{\pi_m(1-n_v) - \pi - \eta}{\pi^{1/\gamma}(\pi_m - 1)}\right]^{2/3} \pi^{\beta + 1/\gamma} - \frac{dn_v}{d\tau} - \frac{d\eta}{d\tau} \tag{33}$$

$$\frac{dn_v}{d\tau} = K_e \pi^{(\gamma+1)/2\gamma}\left(\frac{\pi_a}{\pi}\right)^{1/2\gamma}\left[1 - \left(\frac{\pi_a}{\pi}\right)^{1-1/\gamma}\right]^{1/2} \tag{34}$$

$$\frac{d\eta}{d\tau} = \left(\gamma\pi^{1-1/\gamma} - 1\right)\frac{dn_v}{d\tau} \tag{35}$$

The dimensionless quantity π_m is p_m/p_0 where p_m is the maximum pressure in a closed vessel when adiabatic combustion is assumed. The dimensionless quantity π_a is p_a/p_0 where p_a is atmospheric pressure.

When burned gas is vented, Eqs. (33) to (35) are replaced by

$$\frac{d\pi}{d\tau} = 3\chi(\pi_m - 1)\left[1 - \frac{\pi_m(1-n_v) - \pi - \eta}{\pi^{1/\gamma}(\pi_m - 1)}\right]^{2/3} \pi^{\beta + 1/\gamma} - \pi_m\frac{dn_v}{d\tau} - \frac{d\eta}{d\tau} \tag{36}$$

$$\frac{dn_v}{d\tau} = K_e \pi^{(\gamma+1)/2\gamma}\left(\frac{\pi_a}{\pi}\right)^{1/2\gamma}\left[1 - \left(\frac{\pi_a}{\pi}\right)^{1-1/\gamma}\right]^{1/2}\left(\frac{\rho_b}{\rho_u}\right)^{1/2} \tag{37}$$

$$\frac{d\eta}{d\tau} = \left(\gamma\frac{\pi - (1 - n_v - n_b)\pi^{1-1/\gamma}}{n_b} - \pi_m\right)\frac{dn_v}{d\tau} \tag{38}$$

The dimensionless venting parameter K_e in Eqs. (34) and (37) is defined as

$$K_e = \frac{3C_d}{\pi}\sqrt{\frac{2}{\gamma - 1}}\frac{A_v}{D_v^2}\frac{c}{S_0} \tag{39}$$

where D_v [m] is the vessel diameter and c is sound velocity 340 m/s.

If the vent flow is sonic, that is

$$\pi_a < \pi_c = \pi\left(\frac{2}{\gamma + 1}\right)^{\gamma/(\gamma-1)} \tag{40}$$

then the substitution $\pi_a = \pi_c$ is made in Eqs. (34) and (37).

Crescitelli et al. (1980) use the test data by Harris and Briscoe (1967) of vented explosions of pentane-air mixtures in a 1.7 m^3 nearly spherical vessel ignited at the centre to find the values of the turbulence factor χ assumed constant during each test. Four different vent sizes from 0.02 to 0.29 m^2 and a wide range of vent opening pressures, up to 5.3 bar, were used in the tests. No measurement of the discharge coefficient C_d, however, was made.

To overcome the lack of experimental data of C_d, Crescitelli et al. (1980) decided to determine the modified turbulence factor χ/C_d. This procedure was possible because the same reduced pressures P_{red} are found by dividing the equations by C_d and using a modified dimensionless time $C_d\tau$. The values of χ/C_d determined in this way ranged 5–45. If $C_d = 0.6$ is assumed, the range of the turbulence factor was 3–27. Though the obtained values of χ in general agree with the range of the ratio of S_t/S_0 (where S_t is the turbulent burning velocity), they exceed the values found in similar investigations and are too high for a 1.7 m^3 vented enclosure (Molkov et al. 1993).

The model was developed further by Tufano et al. (1981) so that the dependence of reduced pressure on the parameter A could be investigated. The two systems of differential equations Eqs. (33) to (35) and Eqs. (36) to (38) for unburned and burned gas venting, respectively, were combined in a single one.

$$\frac{d\pi}{d\tau} = 3\chi(\pi_m - 1)\pi^{\beta+1/\gamma}\left[1 - \frac{\pi_m(1-n_v) - \pi - \eta}{\pi^{1/\gamma}(\pi_m - 1)}\right]^{2/3} - [1 - A(\pi_m - 1)]\frac{dn_v}{d\tau} - \frac{d\eta}{d\tau} \quad (41)$$

$$\frac{dn_v}{d\tau} = K_e\left\{\pi\frac{\rho_{av}}{\rho_0}\left(\frac{\pi_m}{\pi}\right)^{1/\gamma}\left[1 - \left(\frac{\pi_m}{\pi}\right)^{1-1/\gamma}\right]\right\}^{1/2} \quad (42)$$

$$\frac{d\eta}{d\tau} = \left\{A\left[\frac{\gamma\left(\pi - (1-n_v-n_b)\pi^{1-1/\gamma}\right)}{n_b} - \pi_m\right] + (1-A)\left(\gamma\pi^{1-1/\gamma} - 1\right)\right\}\frac{dn_v}{d\tau} \quad (43)$$

Due to lack of experimental data Tufano et al. (1981) tested four hypotheses on the value of A

1. Unburned gas venting, A = 0.
2. Burned gas vented, A = 1.
3. A is set equal to the mass fraction of burned gas in the enclosure.
4. A is set equal to the fraction of vessel volume that is occupied with burned gas.

An earlier analysis on the effect of the nature of the vented gases on the available experimental pressure-time patterns had shown the hypotheses 1 and 3 gave unrealistic results, and that the best fit was obtained when the value of A was set close to unity. The inadequacy of hypothesis 1 was also confirmed by analysis of the test data by Harris and Briscoe (1967). Finally, the use of more complex hypothesis 4 appeared not to be justified, because it gave values of χ/C_d not significantly different from those obtained with the simpler hypothesis 2.

Tufano et al. (1981) derive the following correlation for the prediction of χ/C_d

$$\frac{\chi}{C_d} = 0.51K_e^{0.6}\exp\left(-\frac{0.27}{\pi_s^3}\right) \quad (44)$$

where π_s is the dimensionless vent opening pressure $P_{stat}/p_0 + 1$. Molkov et al. (2000) criticise Eq. (44) because modified turbulence factor χ/C_d does not increase with enclosure volume V when the vent coefficient K remains constant. The absence of a dependence on enclosure scale is inexplicable from the physical point of view and does not comply with recent results and understanding of the vented deflagration phenomena.

4.3 The model of Molkov et al.

Molkov et al. (1993) have derived a system of differential equations for vented gas explosions from energy and mass conservation equations, ideal gas equations and the equations of adiabatic compression and expansion in a vessel. An improvement with respect to earlier models is that unburned mixture and burned gases are assumed to have different values of the specific heat ratio: γ_u and γ_b, respectively. The parameters S_r and ε in Eq. (31) giving the pressure dependence of S_0 in an isentropic process can be measured with a closed explosion vessel.

The differential equations for dimensionless rates of change in pressure, burned gas mass and unburned gas mass are

$$\frac{d\pi}{d\tau} = 3\pi \frac{\chi Z \pi^{\varepsilon+1/\gamma_u}\left(1-n_u\pi^{-1/\gamma_u}\right)^{2/3} - \gamma_b W\left[(1-A)R_u + AR_b\left(\frac{\pi^{1/\gamma_u}-n_u}{n_b}\right)\right]}{\pi^{1/\gamma_u} - \frac{\gamma_u-\gamma_b}{\gamma_u}n_u} \tag{45}$$

$$\frac{dn_b}{d\tau} = 3\left[\chi\pi^{\varepsilon+1/\gamma_u}\left(1-n_u\pi^{-1/\gamma_u}\right)^{2/3} - AR_bW\right] \tag{46}$$

$$\frac{dn_u}{d\tau} = -3\left[\chi\pi^{\varepsilon-1/\gamma_u}\left(1-n_u\pi^{-1/\gamma_u}\right)^{2/3} + (1-A)R_uW\right] \tag{47}$$

where

$$Z = \gamma_b\left[E - \frac{\gamma_u}{\gamma_b}\frac{\gamma_b-1}{\gamma_u-1}\right]\pi^{(1-\gamma_u)/\gamma_u} + \frac{\gamma_b-\gamma_u}{\gamma_u-1} \tag{48}$$

The parameters R_u and R_b defined for subsonic vent flow are

$$R_i = \left\{\frac{2\gamma_i}{\gamma_i-1}\pi\frac{\rho_i}{\rho_0}\left[\left(\frac{p_a}{p_0\pi}\right)^{2/\gamma_i} - \left(\frac{p_a}{p_0\pi}\right)^{1+1/\gamma_i}\right]\right\}^{1/2} \tag{49}$$

where the subscript i is either u or b for unburned and burned gas, respectively. For sonic flow, when

$$\pi \geq \frac{p_a}{p_0}\left(\frac{\gamma_i+1}{2}\right)^{\gamma_i/(\gamma_i-1)} \tag{50}$$

the parameters R_u and R_b are

$$R_i = \left[\gamma_i\left(\frac{2}{\gamma_i+1}\right)^{(\gamma_i+1)/(\gamma_i-1)}\pi\frac{\rho_i}{\rho_0}\right]^{1/2} \tag{51}$$

where the subscript i is either u or b for unburned and burned gas, respectively.

The venting parameter W is defined as

$$W = \frac{0.207}{\sqrt{\gamma_u}}\frac{\mu A_v}{V^{2/3}}\frac{c}{S_0} \tag{52}$$

The essential difference of W Eq. (52) and the venting parameter K_e Eq. (39) used by Crescitelli et al. (1980) is that the discharge coefficient μ is not a constant, which can be determined independently of the explosion tests, but a variable.

Molkov et al. (1993) present also a fourth differential equation complementing Eqs. (45) to (47) in the simulation of those tests where the test vessel was not vented into the atmosphere but into another, vacuumed vessel.

A simple analysis of explosion dynamics equations for a closed vessel (W = 0) shows that the cube rule law Eq. (15) does not work and should be modified to include the turbulence factor

$$\left(\frac{dP}{dt}\right)_{max} \frac{V^{1/3}}{\chi} = const. \tag{53}$$

It follows from Eqs. (45) to (52) that gas combustion dynamics in a vented vessel with known gas properties is determined only by two unknown parameter, namely turbulence factor χ and discharge coefficient μ. Gas properties and vessel parameters are included in the dimensionless parameters W, β, γ_u, γ_b, E, π_s and π_a. Besides, discharge coefficient μ is the only unknown in the venting parameter W.

Molkov et al. (1993) applied the model to a large data set of vented gas explosions in vessels of volume from 0.02 to 11 m^3 with near-stoichiometric mixtures of acetone, propane or hexane to determine the turbulence factor χ and discharge coefficient μ. This was possible because

1. when χ/μ = constant, the reduced pressure P_{red} does not change and the time of its attainment varies proportional to $1/\chi$
2. when μ = constant, the rise in χ causes the rise in P_{red} and a decrease in time of its attainment.

The method to determine the values of χ and μ on the basis of measured P_{red} and the time of its attainment are illustrated in Figure 4.

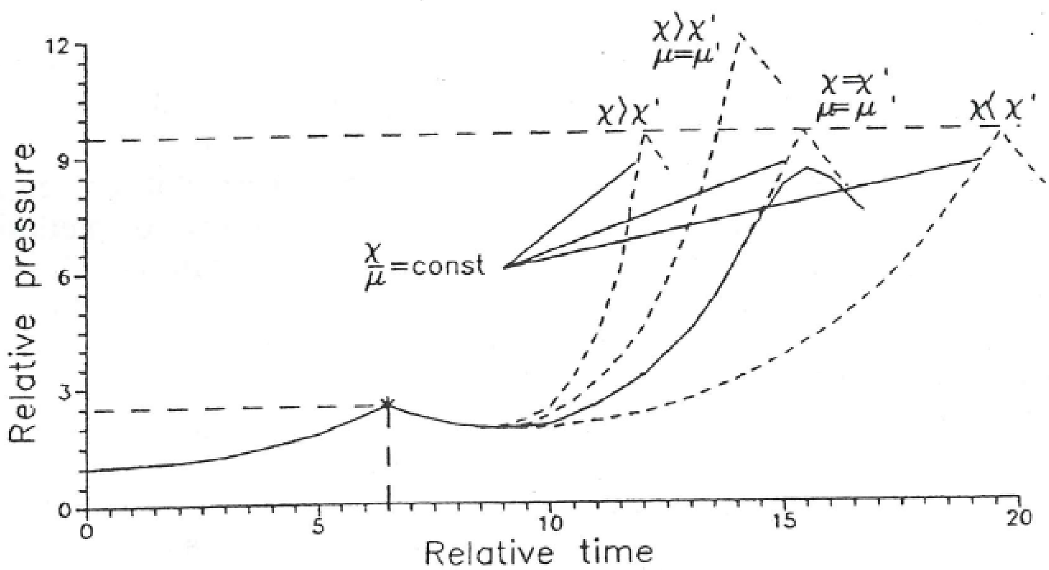

*Figure 4. Influence of turbulence factor χ and discharge coefficient μ on the dynamics of vented gas explosion. * = vent opening, χ' and μ' = optimal values, dashed curve = model, solid curve= experiment (Molkov et al. 1993).*

Analysis of the high-speed movies taken from the tests revealed that a mixture of unburned and burned gas was vented when vent opening pressure P_{stat} was low and the mixture was ignited at the centre of the vessel. The value of the parameter

A was approximately equal to the ratio of the area of the equivalent spherical flame front to the vessel area or $A = (r_f/a)^2$. When the mixture was ignited near the vent, only burned gas vented (A = 1). This was also the case for values of P_{stat} higher than p_0.

According to the model, the reduced pressure P_{red} is proportional to $(\chi/\mu A_v)^2$. Modelling of the test explosions showed that in a vented gas explosion the gas combustion dynamics responds to external changes in process conditions in such a way as to counteract the effect of external influence. When the vent area A_v in a 11 m^3 vessel was increased tenfold to decrease P_{red} , the turbulence factor increased twofold. That is, P_{red} decreased by the factor of five, only. When explained in physical terms, the disturbing influence on the flame front increases with flame area. This counteracting occurs when χ is larger than 5. At high turbulence inside the vessel the discharge coefficient μ may be somewhat larger than 1.

Molkov et al. (1993) present some preliminary correlations for the estimation of χ:
Vessels of volume $V \le 10$ m^3 and vent coefficient $K \le 0.25$

$$\chi = (1+0.15V)(1+4K) \tag{54}$$

Vessels of volume $V \le 200$ m^3 and subsonic venting ($P_{stat} < p_0$):

— uncovered vent $\chi = 2$

— covered vent $\chi = 8$

Vessels of volume $V \le 200$ m^3, uncovered vent and sonic venting ($p_0 \le P_{stat} < P_m$):

$$\chi = 0.8 + 1.2\frac{\pi_m - \pi_r}{\pi_m - 2} \tag{55}$$

Vessels of volume $V \le 200$ m^3, covered vent and sonic venting ($p_0 \le P_{stat} < P_m$):

$$\chi = 2 + 6\frac{\pi_m - \pi_r}{\pi_m - 2} \tag{56}$$

Vessels of volume $V \le 10$ m^3 vented through a duct with vent coefficient $K \le 0.04$ and subsonic venting ($1 < \pi_r < 2$): $\chi = 4$.

Molkov et al. (2000) validate the method with data of vented hydrocarbon-air and hydrogen-air explosions. When the values of χ and μ are selected on the basis of measured P_{red} and the time of its attainment, the predicted overpressure vs. time curves comply reasonably well with the measured ones (Figures 5 and 6).

In Figs. 5 and 6, some discrepancy between experimental and theoretical deflagration dynamics is seen at the last stages of combustion, especially at low pressures. Part of the difference between experimental and calculated explosion pressures at the final stages of combustion can be explained in particular by the process generally adopted in the models of neglecting heat losses to the enclosure walls due to the short duration of the process.

Some oscillatory phenomena that are observed experimentally cannot be described by the lumped parameter theory. It should be underlined that the computational fluid dynamics (CFD) approach also cannot predict these oscillations at the final stage of deflagration caused by flame instability (Molkov et al. 2000).

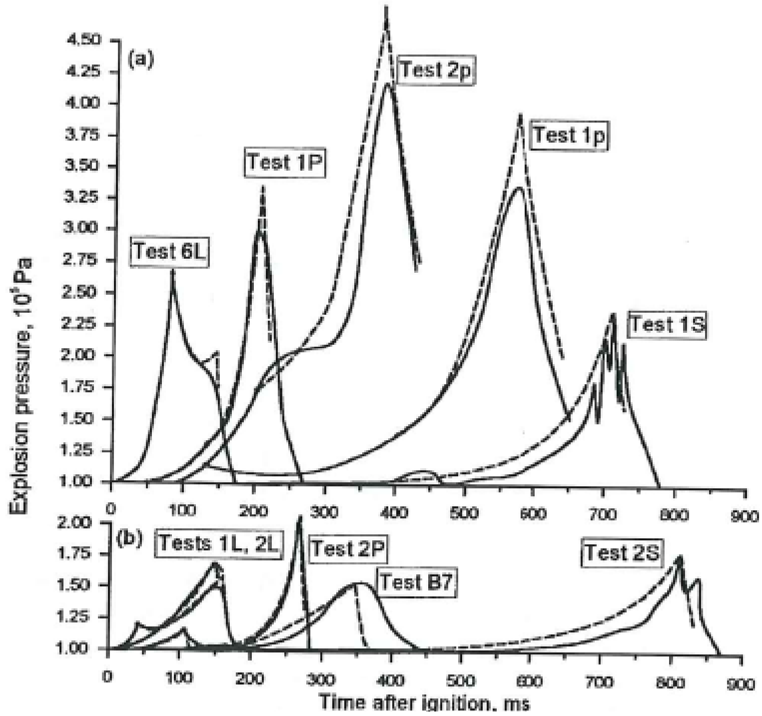

Figure 5. Hydrocarbon-air vented explosions for high (a) and medium (b) reduced pressures: experiments (solid lines) and theory (dashed lines) (Molkov et al. 2000).

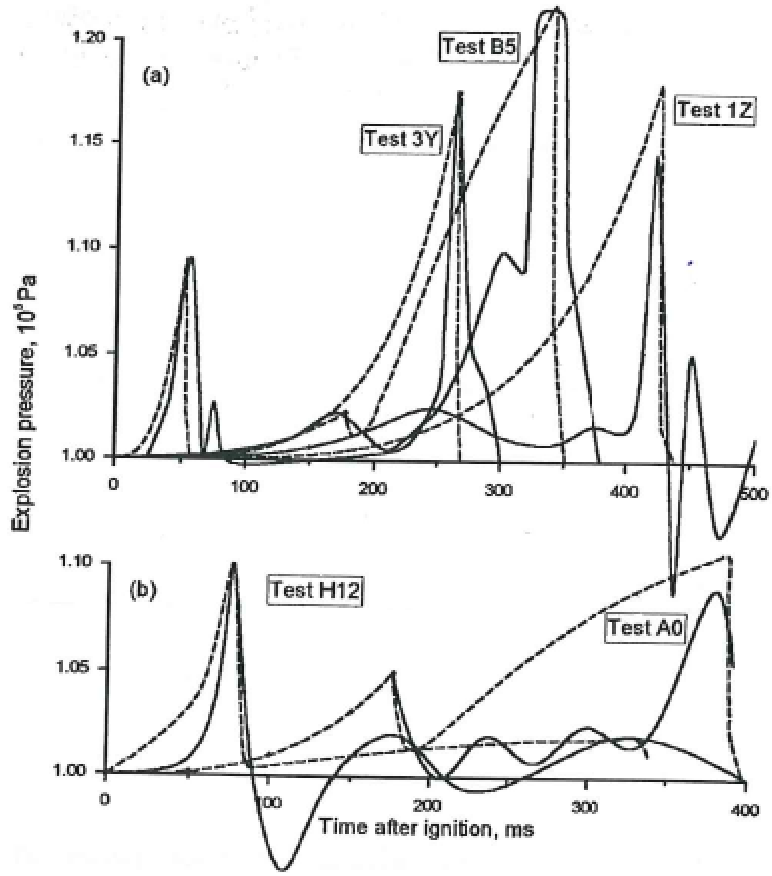

Figure 6. Hydrocarbon-air vented explosions for low (a) and extremely low (b) reduced pressures: experiments (solid lines) and theory (dashed lines) (Molkov et al. 2000).

5 Semi-empirical methods

5.1 The method of Bradley and Mitcheson

Bradley and Mitcheson (1978a) develop a simplified model of vented gas explosion for the purpose of processing the available test data into simple formulas for vent area dimensioning. They assume that a spherical vessel is filled with a homogenous mixture of ideal gases which is ignited at the centre. There is no energy loss to the vessel wall, and the pressure at any instant is equalised throughout the vessel. All compression and expansions within the vessel are isotropic. The rate of volume generation in the vessel dV/dt is given by Eq. (6).

The initial pressure in the vessel is atmospheric pressure p_a. When the vent opening pressure $p_{stat} = P_{stat} + p_a$ is attained, the vent covering is removed instantaneously and does not impede the vent flow. During the venting, the flame front remains spherical and either unburned or burned gases are vented. The minimum vent area A_v is defined so that it is just sufficient to vent the amount of gas given by Eq. (6).

Bradley and Mitcheson (1978a) define a dimensionless vent area ratio

$$\bar{A} = C_d \frac{A_v}{A_s} \tag{57}$$

where A_s is the total area of the spherical vessel [m^2] and a dimensionless burning velocity which only depends upon the initial mixture

$$\bar{S}_0 = \frac{S_0}{c}(E-1) \tag{58}$$

The resulting general criterion for subcritical vent flow is

$$\frac{\bar{A}}{\bar{S}_0} \geq \left\{ \frac{2}{\gamma_i - 1} \left(\frac{p_a}{p_{stat}} \right)^{2/\gamma_i} \left[1 - \left(\frac{p_a}{p_{stat}} \right)^{1-1/\gamma_i} \right] \right\}^{-1/2} \zeta_i \tag{59}$$

and for critical vent flow

$$\frac{\bar{A}}{\bar{S}_0} \geq \left(\frac{\gamma_i + 1}{2} \right)^{\frac{\gamma_i + 1}{2(\gamma_i - 1)}} \zeta_i \tag{60}$$

where the subscript i is either u or b for unburned and burned gas, respectively.

The quantity ζ_i for unburned gas venting is

$$\zeta_u = \frac{S_v}{S_0} \frac{\rho_{uv}/\rho_{uv} - 1}{E - 1} \left(\frac{p_a}{p_{stat}} \right)^{\frac{\gamma_u - 1}{2\gamma_u}} \tag{61}$$

and for burned gas venting

$$\zeta_b = \zeta_u \left(\frac{\gamma_u}{\gamma_b} \frac{\rho_{bv}}{\rho_{uv}} \right)^{1/2} \tag{62}$$

Here the subscript v means that the quantities S_0, ρ_u and ρ_b must be evaluated at vessel pressure assumed equal to the vent opening absolute pressure p_{stat}.

Bradley and Mitcheson (1978b) use Eqs. (59) to (62) and a large amount of test data to derive correlations relating the dimensionless parameter $\bar{A}/\bar{S}_0$ with the experimental values of P_{stat} and P_{red}. The value of the discharge coefficient C_d is fixed at 0.6. The correlations can be used to estimate the minimum vent area for initially open and initially covered vents. For initially open vents, Bradley and Mitcheson (1978b) give the following correlation valid for P_{red} < 1.01 bar

$$\frac{\bar{A}}{\bar{S}_0} \geq \left(\frac{0.71\,bar}{P_{red}}\right)^{1/2} \tag{63}$$

For initially covered vents, when P_{stat} < 1.01 bar

$$\frac{\bar{A}}{\bar{S}_0} \geq \left(\frac{12.46\,bar}{P_{stat}}\right)^{1/2} \tag{64}$$

when P_{stat} > 1.01 bar

$$\frac{\bar{A}}{\bar{S}_0} \geq \left(\frac{2.43\,bar}{P_{stat}}\right)^{1.43} \tag{65}$$

Eqs. (64) and (65) have been derived assuming $P_{red} = P_{stat}$. When $P_{red} > P_{stat}$, the correlation by Bradley and Mitcheson (1978b) can be written as

$$\frac{\bar{A}}{\bar{S}_0} \leq 3.51\frac{P_{stat}^{0.3}}{P_{red}^{0.8}} \tag{66}$$

5.2 The method of Molkov

The purpose of Molkov and co-workers was to use the model presented above to generalise test data to derive simple experimental correlations that could be used to dimension vent areas. For this purpose Molkov et al. (2000) define a new dimensionless parameter similar to $\bar{A}/\bar{S}_0$. The only change is to replace the area of the spherical vessel A_s by $V^{2/3}$. The new parameter is called Bradley number Br

$$Br = \frac{A_v}{V^{2/3}}\frac{c}{S_0(E-1)} \tag{67}$$

When different values for the specific heat ratio γ are used for unburned and burned gas, Bradley number is defined as

$$Br = \frac{A_v}{V^{2/3}}\frac{c}{S_0\left(E - \frac{1-1/\gamma_b}{1-1/\gamma_u}\right)} \tag{68}$$

Subsequently, the turbulent Bradley number Br_t containing the ratio of the turbulence factor χ to the generalised discharge coefficient μ is defined as

$$Br_t = 0.207\sqrt{\frac{E}{\gamma_u}}\frac{Br}{\frac{\chi}{\mu}} \tag{69}$$

Molkov et al. (2000) propose a universal correlation for the dimensionless reduced pressure P_{red}/p_0 as a function of Br_t

$$\frac{P_{red}}{p_0} = Br_t^{-2.4} \tag{70}$$

Eq. (70) is valid for $P_{red} \leq p_0$ and $Br_t \geq 1$. For the opposite case i.e. $P_{red} > p_0$ and $Br_t < 1$ the correlation is

$$\frac{P_{red}}{p_0} = 7 - 6Br_t^{0.5} \qquad (71)$$

According to the model of Molkov et al. (1993), the dimensionless reduced pressure P_{red} is proportional to $(\chi/\mu A_v)^2$. Le Chatelier-Brown principle analogue for vented explosions states that the hydrodynamics of gaseous combustion in a vented vessel responds to external changes in process conditions in such a way as to weaken the effect of the external influence. The obvious idea exists to reduce explosion overpressure by increasing vent area A_v. However, according to the stated principle, the increase of A_v is always accompanied by an increase of the ratio χ/μ, called also the deflagration-outflow interaction number DOI.

As a result, the effective increase of the vent area, which is $A_v/(\chi/\mu)$, is often much less than expected. For example in experiments with propane-air mixtures by Pasman et al. (1974), the twofold increase of A_v was accompanied by an increase of 1.57 times in χ/μ. For hydrogen-air mixtures, a 1.5 times increase of A_v was accompanied by a 1.25 times increase of χ/μ. In the experiments with hydrogen-air mixtures by Kumar et al. (1989), a ninefold growth of A_v was accompanied by an increase in χ/μ of 2.79 times for a 10 % mixture and 2.93 times for a 20 % mixture.

If explosion conditions, like position of the ignition source and vent opening pressure, do not differ significantly for a given enclosure, the following has been obtained from the processing of experimental data and agrees with the Le Chatelier-Brown principle analogue.

- The large increase of μ by 2.25 times with vent diameter increase from 15 cm to 45 cm, due to the influence of duct in the experiments by Kumar et al. (1989), was accompanied by a decrease of 1.12 times in the turbulence factor χ.
- A 1.5 times increase of χ in the experiments by Yao (1974) was compensated partially by an increase of 1.25 times in the generalised discharge coefficient μ.
- A sixfold increase in the laminar burning velocity S_0, when the hydrogen concentration changed from 10 % to 20 % in the experiments by Kumar et al. (1989), was accompanied by a slight 1.15 times decrease of the turbulence factor χ.

Very simple physical ideas underlie the principle being discussed. It is easy to imagine that an increase of vent area with all other conditions the same should increase the disturbance of the flame front and hence the value of the turbulence factor χ. Additionally, the growth of χ can be associated with the increase of μ due to a higher flow velocity (it is well known that the discharge coefficient grows with flow velocity). A decrease in μ could be a reason for the decrease of χ due to the relative decrease of outflow velocity.

According to fractal theory, the measured area of a surface depends on a measurement scale ϕ as a power law: $A \sim \phi^{2-D}L^D$. To obtain a relationship for the turbulence factor, the real surface area A is divided by the area of the corresponding spherical surface that is proportional to L^2. Then $\chi \sim (L/\phi)^{D-2}$. The fractal character of homogeneous turbulence has been studied by measuring the fractal dimension of clouds and by analysing turbulent dispersion. A value of approximately 2.35 to 2.40 was suggested for the fractal dimension D. The value D = 2.4 was found when a geometrical correlation for χ/μ was developed based on test results in empty enclosures as well as enclosures with obstructions inside. That is, $\chi \sim (L/\phi)^{0.4}$.

The following correlation for the dependence of χ/μ on enclosure volume V [m^3], Bradley number Br and dimensionless vent opening pressure π_s has been derived by Molkov et al. (2000)

$$\frac{\chi}{\mu} = B\left[\frac{\left(1+10V^{1/3}\right)\left(1+0.5Br^{b}\right)}{2+\dfrac{P_{stat}}{p_0}}\right]^{0.4} \tag{72}$$

The empirical coefficients for hydrocarbon-air mixtures are B = 1.75 and b = 0.5, and those for hydrogen-air mixtures are B = 1.0 and b = 0.8. Unlike the universal correlation, Eqs. (70) and (71), which originates from the theory of vented explosions, Eq. (72) is semi-empirical correlation and represents a best fit of data on χ/μ obtained by the inverse problem method.

Molkov (2001a) generalised Eqs. (70) to (72) to cover cases where the initial pressure p_0 is higher than atmospheric pressure p_a.

The dimensionless reduced pressure P_M is defined as

$$P_M = \frac{\dfrac{p_{red}}{p_0}-1}{\left(\dfrac{p_{stat}}{p_0}\right)^{3/2}} \tag{73}$$

where p_{red} and p_{stat} [bar] are the absolute explosion pressure and vent opening pressure, respectively.

When $P_M \leq 1$ and $Br_t \geq 1$, the correlation is

$$P_M = Br_t^{-2.4} \tag{74}$$

In the opposite case, i.e. $P_M > 1$ and $Br_t < 1$, the correlation is

$$P_M = 7-6Br_t^{0.5} \tag{75}$$

The correlation for χ/μ Eq. (72) is changed to the form as follows

$$\frac{\chi}{\mu} = B\left[\frac{\left(1+10V^{1/3}\right)\left(1+0.5Br^{b}\right)}{1+\dfrac{p_{stat}}{p_0}}\right]^{\gamma} p_0^{0.6} \tag{76}$$

where the unit of p_0 is bar. If the initial pressure p_0 is 1 bar, Eq. (76) is identical to Eq. (72).

Molkov (2001b) stresses that Eqs. (74) and (75) were obtained by the best fit to experimental data. Noticeable experimental data scattering was seen at high level of turbulence for large-scale experiments in a segment form 4000 m^3 enclosure (Harrison & Eyre 1987) and the explosion incident in a 8087 m^3 building (Molkov

1999). However, usually conservative rather than mathematically accurate methods are used by explosion safety practitioners. Molkov (2001b) derives a conservative form of the universal correlation to cover all the experimental points. The dimensionless reduced pressure P_C is defined as

$$P_C = \frac{P_{red} / p_0}{(P_{stat} / p_0 + 1)^{5/2}} \qquad (77)$$

When $P_C \leq 1$ and $Br_t \geq 2$, the correlation is

$$P_C = 5.65 Br_t^{-2.5} \qquad (78)$$

In the opposite case i.e. $P_C > 1$ and $Br_t < 2$ the correlation is

$$P_C = 7.9 - 5.8 Br_t^{0.25} \qquad (79)$$

The correlation estimate for the ratio χ/μ Eq. (76) has been employed to design the correlations Eqs. (78) and (79) and hence must be used along with them in vent sizing.

Molkov et al. (2004) extend the method to cover enclosures with inertial vent covers. They observe that information on the maximum value of surface density w for which the effect of vent inertia has a negligible effect on P_{red} is contradictory: values of w from 6 kg/m^2 to 120 kg/m^2 have been given in different sources. The sources do not state how the given values depend on V, A_v and combustible mixture.

Molkov et al. (2004) modify the experimental correlation by Cubbage and Marshall Eq. (10) by multiplying the laminar burning velocity S_0 by the ratio χ/μ and readjusting the value of the numerical coefficient to experimental data (the overpressures P_1 and P_{stat} are expressed in bar)

$$P_1 = P_{stat} + 4.3 \cdot 10^{-4} \left(S_0 \frac{\chi}{\mu} \right)^2 \frac{A_{cs}}{A_v} \frac{w}{V^{1/3}} \qquad (80)$$

where A_{cs} [m^2] is the area of the enclosure cross section which is parallel to the wall with vent panel.

According to results available to Molkov et al. (2004), the second pressure peak P_2 does not depend on the inertia of the vent cover if the vent area is fully opened before completion of the explosion. When P_{red} is no more than 1 bar and the initial pressure p_0 is equal to atmospheric pressure, Eq. (78) can be used to calculate the second pressure peak (the overpressure P_2 is expressed in bar)

$$P_2 = 1 + 5.65 \left(\frac{P_{stat}}{Br_t} \right)^{5/2} \qquad (81)$$

where the turbulent Bradley number Br_t Eq. (69) must be equal to or larger than 2.

6 Venting of partial volume deflagrations

6.1 Flame propagation in stratified or layered mixtures

Existing correlations for industrial systems, equipment and buildings rely on experimental observations and theoretical analyses reported in the literature in recent decades for full volume, well-mixed explosions. However, practically no engineering correlations for vent sizing are given in the literature for partially filled volume explosions.

Accidental indoor spills of volatile solvents and the release of flammable gases within buildings and equipment are likely to form flammable mixtures with air if the mixing is inefficient or the ventilation rate is inappropriate. Concentration gradients are formed unless efficient mixing is provided. As a consequence, even small quantities of fuel can form stratified (to the floor) on layered (to the ceiling) flammable vapour or gas mixtures.

If these mixtures are ignited, partial volume deflagrations may occur, possibly producing peak overpressures capable of provoking significant structural damage. Even pockets of flammable mixture occupying a small portion of the total volume are able to produce significant damage in low-strength enclosures.

Flame propagation in stratified mixtures is a very complex phenomenon. The co-existence of three combustion modes can occur (Figure 7). Several experimental observations have demonstrated that the first laminar premixed combustion front travels across the layer of fuel-air mixture that is initially between the upper (UFL) and lower (LFL) flammability limits.

If a rich flammable layer has been formed, the premixed flame front is followed by a diffusive flame along the stoichiometric line, between the excess of fuel in the rich layer and the excess of air in the lean layer. If a layer outside the upper flammability limit has been formed, a convective flame is established, after a certain distance, due to the convective mixing of the unburned fuel with air, ignited by the hot products of the previous combustion modes. The entire three flames structure extends on a dimension greater than the flammable layer thickness (Orlando et al. 2007).

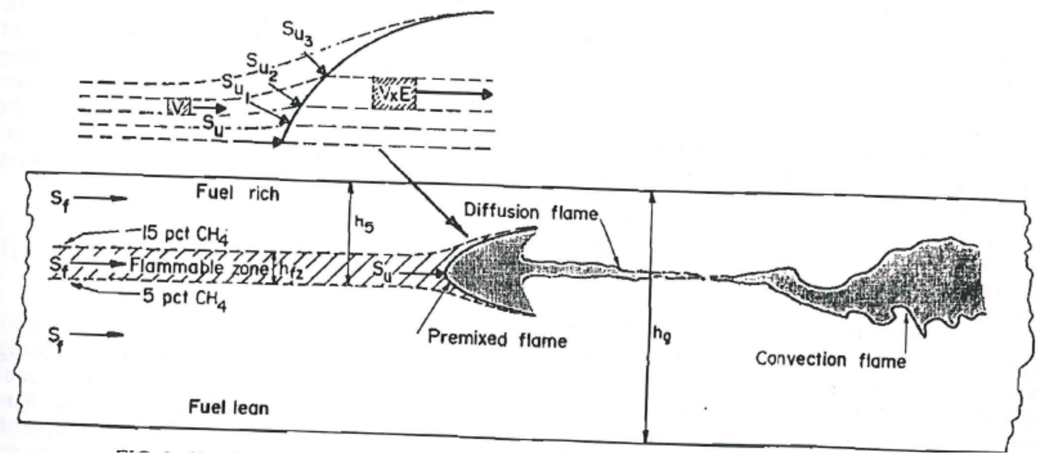

Figure 7. Sketch of the three combustion modes in a layered methane-air mixture (Liebman et al. 1970).

A useful simplifying approximation is to consider that the entire amount of flammable gas or vapour forms a homogenous mixture of stoichiometric concentration. The homogenous mixture is then characterised by a thickness h, to compare with the height of the enclosure H in order to define a non-dimensional filling ratio parameter m = H/h.

In elongated open tunnels, flame propagation through layers with h << H, experimentally shows a "steady" flame speed about four times S_0 of the stoichiometric mixture. This effect is mainly related to the increase of flame front area due to purely hydrodynamic effects induced by the gas explosion. For thicker flammable layers, the flame front experiences stronger acceleration as h approaches H. Nevertheless, in the configurations investigated, which do not include the presence of obstacles, flame propagation was always observed to follow a laminar regime.

Liebman et al. (1970) have performed flame propagation tests is a laboratory scale gallery, 2.44 m long, 76 mm wide and 152 to 254 mm high. The gallery was constructed of transparent plastic sheets. The gallery was divided into two compartments with a separator sheet. The upper compartment was filled with homogenous fuel-rich methane-air mixture. When the separator was withdrawn, the gases interdiffused forming a well-defined combustible zone along the entire length of the gallery. The methane concentration at the gallery roof was always kept above UFL so that the flammable zone through which the premixed flame propagated was always bound by the LFL and UFL isopleths.

At a predetermined time after removing the separator sheet, the flammable mixture was ignited at the closed end of the gallery. At the opposite end, the upper compartment was closed to eliminate convective flow of the low density roof layer, although the lower section remained open. Flame propagation along the layer was followed by filming at 100 frames/s.

Feng et al. (1975) have pursued the tests by Liebman et al. (1970) in a similar gallery. They have evaluated the effect of the parameter m on flame speed and hence the rate of pressure rise. The result is shown in Figure 8. For values of m > 10, flame speed is almost constant, while for m < 10, quite significant increase occurs with increasing distance x/L, where L is the length of the gallery. The constant value of flame speed for m > 10 in Fig. 8 is about 1.85 m/s or 5 times the laminar burning velocity of stoichiometric methane 0.37 m/s.

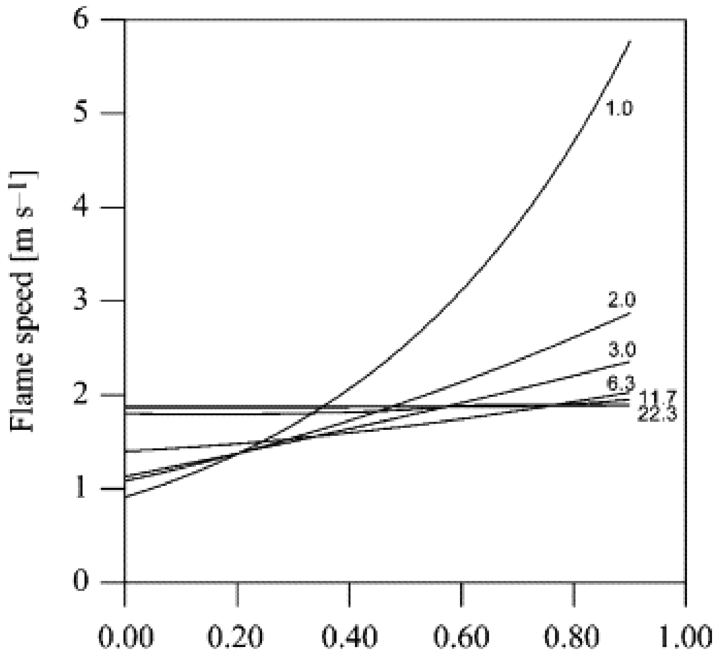

Figure 8. Measured flame speed as a function of distance x/L for several values of the parameter m (Feng et al. 1975).

Feng et al. (1975) develop a model describing flame propagation in layered mixtures and conclude that the increased flame speed (with respect to the laminar flame speed) appears to be predominantly due to a fluid dynamic interaction resulting from the combustion of the premixed gas layer. That is, after the flammable gas moves through the flame front, the expansion of the gas due to combustion causes a displacement of the initial flammable gas layer over a much larger curved surface on the curved flame front. Thus, the expansion of the gas sustains the pressure difference across the flame front, and the resulting larger flammable gas area increases the burning rate necessary for the elevated flame speed.

Kaptein and Hermance (1977) investigated flame propagation in a stratified layer of vapour above a pool of flammable liquid. Such a stratified layer is formed also when a flammable liquid is absorbed in a porous ground. The latter situation was simulated in laboratory with a flame trough constructed of long, parallel polycarbonate walls, end plates, and a lower evaporation surface. The trough, 240 cm long, 8 cm wide and 25 cm high, was open on the top and had a wire mesh bottom to support the 100 μm glass beads filling it to a level, uniform depth.

It could be lowered into a tank containing a liquid fuel to a depth which just wetted the bottom of the beads. The fuel was transported to the top of the bed by capillarity and its vapour diffused into the stagnant region between the side walls and the end plates. Diffusion was allowed for varying lengths of time, depending on how thick a layer of flammable fuel-air mixture was desired. Fuel mole fraction distribution was measured as a function of height by light absorption. The mixture was ignited with a hot wire.

The average flame speeds v_f for n-hexane, n-heptane, benzene and methanol measured by Kaptein and Hermance (1977) are given in Table 1 along with those measured by Liebman et al. (1970) for methane, propene, propane and butane. Kaptein and Hermance (1977) also develop a model that is able to predict the observed flame speeds within 10 % or less.

Table 1. Measured flame speeds in stratified and layered mixtures

fuel	h, cm	m	S_0, m/s	v_f, m/s	v_f/S_0
methane	0.75	33	0.374	1.37	3.65
methane	1.90	13	0.374	1.89	4.95
propene	1.50	17	0.466	1.85	3.90
propane	0.25	100	0.43	1.30	3.00
propane	1.15	22	0.43	1.57	3.80
propane	1.50	17	0.43	1.65	4.10
butane	0.25	100	0.417	1.26	3.05
butane	1.15	22	0.417	1.52	3.65
butane	1.50	17	0.417	1.72	3.75
n-hexane	1.7	15	0.426	1.81	4.35
n-heptane	2.7	9	0.426	2.13	5.20
benzene	1.5	17	0.447	1.80	5.45
methanol	2.4	10	0.524	2.25	5.70

Kaptein and Hermance (1977) reformulate moderately the model by Feng et al. (1975) to cover flame propagation in stratified mixtures, for which the earlier model predicts a value about 1.6 for the ratio of observed to predicted flame speed. They conclude that the high flame speed appears to be the result of purely gas dynamic interactions between the propagating front and the external, non-flammable atmosphere. The front, however, can retain a laminar character and still exhibit propagation velocities that are several times larger than the normal laminar flame speed. A simple theoretical model is presented which quite accurately predicts flame speed, providing the thickness of the flammable layer is known.

Minor effects were observed by changing the mixture composition in the flammable layer. Liebman et al. (1970) found that the flame speed varied as a function of $(h_f/h)^{0.17}$ where h_f is the thickness of the real flammable layer. Therefore, with $h_f >$ h, a slightly faster flame propagation (10 % maximum) can be observed due to the enlarged flame front.

In addition, the distance of the flammable layer from the roof (or from the floor with dense gases) has to be considered. If the total amount of the fuel released is able to form a rich layer with concentration higher that UFL, expansion of the flame front occurs more easily. Moreover, during flame propagation, part of the fuel may be diluted to within flammability limits, thus further increasing the available energy and flame speed. Liebman et al. (1970) have also investigated this effect and never observed changes in flame speed greater than 40 %. Therefore, m is shown to be the main parameter governing variations in flame speed.

In this regard, it is important to note that the inclusion within the flammable layer of released fuel that is initially at concentration which falls outside the flammability range decreases the actual value of m. It thus has the effect of increasing the flame speed with respect of the original calculation. It is also worth mentioning that the initial flame speed is lower for a thick layer (i.e. with low m) than for layers with a higher value of m (see Fig. 8). Hence, at least for the initial phase of the explosion, the reactivity of very thin layers can be seen to be higher. This observation is important because venting efficiency is required especially in the early phases of flame propagation (Orlando et al. 2007).

The analysis above is valid for both open and closed tunnels, and for stratified (to the floor) on layered (to the ceiling) explosions, as demonstrated by Liebman et al. (1970).

6.2 The model of Tamanini

The first model of vented explosions of partially filled enclosures was developed by Tamanini (1996). The model was based on an earlier full volume model for vented gas or dust explosions. The latter model postulated a highly idealised set of conditions: spherical symmetry, thin constant velocity flame front, isothermal unburned and burned gases etc. The composition of the vented flow was set equal to that of the material in the vented enclosure. The reactivity of the mixture was characterised by the burning velocity S_0. Effects of turbulence were taken into account by appropriately modifying S_0. In part because of these assumptions, the full volume model was used as a tool of data correlation rather than a means to provide an accurate representation of the complex phenomena that characterise vented explosions.

A flame propagating at a constant burning velocity causes the pressure in a closed volume to increase at a continuously increasing rate. In absence of heat losses, this results in the maximum rate of pressure rise to occur when the flame front reaches the bounding walls, at which time the maximum pressure is also reached. This behaviour, however, is not realistic, since the peak rate of pressure rise is normally observed before the explosion pressure maximum.

An arbitrary assumption was made to make the calculated pressure reproduce this qualitative feature of the experimental profiles. It was assumed that the burning velocity starts to decrease after the flame front reaches a certain fraction of the flame radius. This modification introduced another adjustable parameter in the model, the value of which was chosen with guidance from experimental data.

The extension of the full volume model to the case of vented partial volume explosions presented some straightforward issues and others that are more subtle.

1. In the first category is the treatment of the fact that only a fraction of the initial volume is reactive.
2. The question of how to slow down the flame front as it reaches the boundary of the flammable mixture is more complex and open to different treatments. A solution to this problem, which has been found to be quite adequate for engineering calculations, is implemented in the model.
3. The question of the shape and location of the flammable mixture is also complex. The implicit assumption is one of geometric similarity and centralised location of the flammable mixture inside the enclosure. This leads to limitations of the predictive capabilities of the approach.

A set of predictions from the partial volume model is shown in Figure 9.

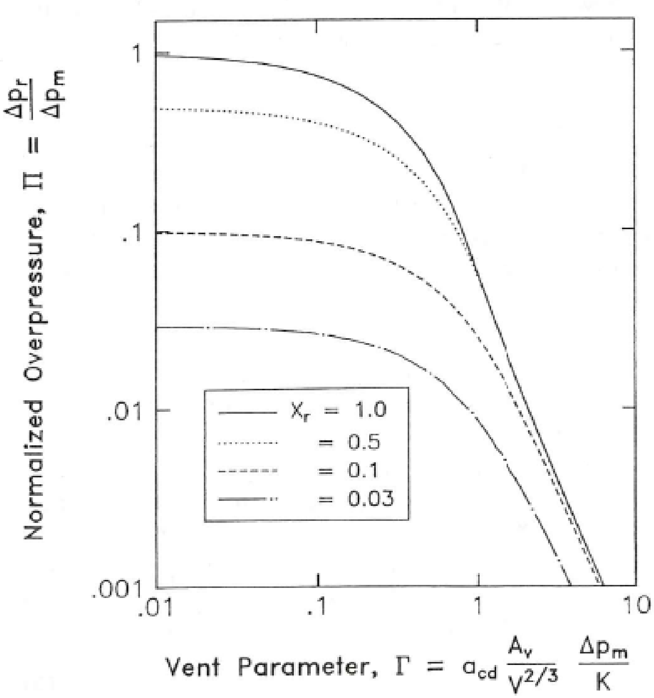

Figure 9. Reduced explosion overpressure predicted by the partial volume model for different values of the filled fraction X_r (Tamanini 1996).

The cases considered correspond to conditions where different fractions of the enclosure volume are filled with the flammable mixture. The filled fraction X_r defines the reactive portion of the total volume. The variables on the coordinate axes are the vent parameter Γ defined as

$$\Gamma = a_{cd} \frac{A_v}{V^{2/3}} \frac{P_m}{K} \tag{82}$$

where a_{cd} [m/s] is a constant

$$a_{cd} = C_d \sqrt{\gamma \frac{\gamma + 1}{2} \frac{RT}{M}} \tag{83}$$

where R is the universal gas constant 8.314 J mol^{-1} K^{-1} and M is the average molecular mass of the flammable mixture [kg/mol], K is the deflagration index, and normalised pressure rise Π defined as

$$\Pi = \frac{P_{red}}{P_m} \tag{84}$$

The predictions shown in Fig. 9 are for three values of partial filling (X_r = 0.5, 0.1 and 0.03) and for a totally filled enclosure (X_r = 1).

A change of the variable Π is suggested by the fact that the maximum overpressure for unvented conditions P_m is proportional to the reacting mass and, therefore, to the filled fraction X_r. This leads to the choice of dividing the parameter Π by X_r, thereby introducing the modified overpressure parameter Π_r

$$\Pi_r = \frac{1}{X_r} \frac{P_{red}}{P_m} \tag{85}$$

The model equations do not give any obvious indications on what should be done with the vent parameter Γ. After a series of trials, Tamanini (1996) found that approximate collapse of the model predictions for partial volume effects could be achieved by multiplying the parameter Γ by $X_r^{1/3}$, leading to the introduction of the modified vent parameter Γ_r

$$\Gamma_r = a_{cd} \frac{A_v}{V^{2/3}} \frac{P_m}{K} X_r^{1/3} \tag{86}$$

The result of using Eqs. (85) and (86) is shown in Figure 10. It can be seen that the overall agreement among the curves corresponding to the different filled fractions X_r is satisfactory. The practical conclusion of this generalisation is that the same vent sizing curve which applies to full volume explosions can be used for partial volume explosions if the parameters Γ_r and Π_r are substituted for Γ and Π, respectively.

For low-strength enclosures P_{red} < 0.1 bar and, consequently, Π_r < 0.013. In this range of values of Π_r, the curves in Fig. 10 can be approximated by the power law

$$\Pi_r = 0.05 \Gamma_r^{-2} \tag{87}$$

In fact, Eq. (87) is a satisfactory approximation also for 0.013 < Π_r < 0.4, that is for high-strength enclosures with 0.1 bar < P_{red} < 2.5 bar.

Inserting Eqs. (85) and (86) into Eq. (87) and solving for A_v, one finds

$$A_v = \frac{K V^{2/3}}{a_{cd} P_m} \left(0.05 \frac{P_m}{P_{red}} \right)^{1/2} X_r^{1/6} = \frac{0.224 K V^{2/3}}{a_{cd} (P_m P_{red})^{1/2} m^{1/6}} \tag{88}$$

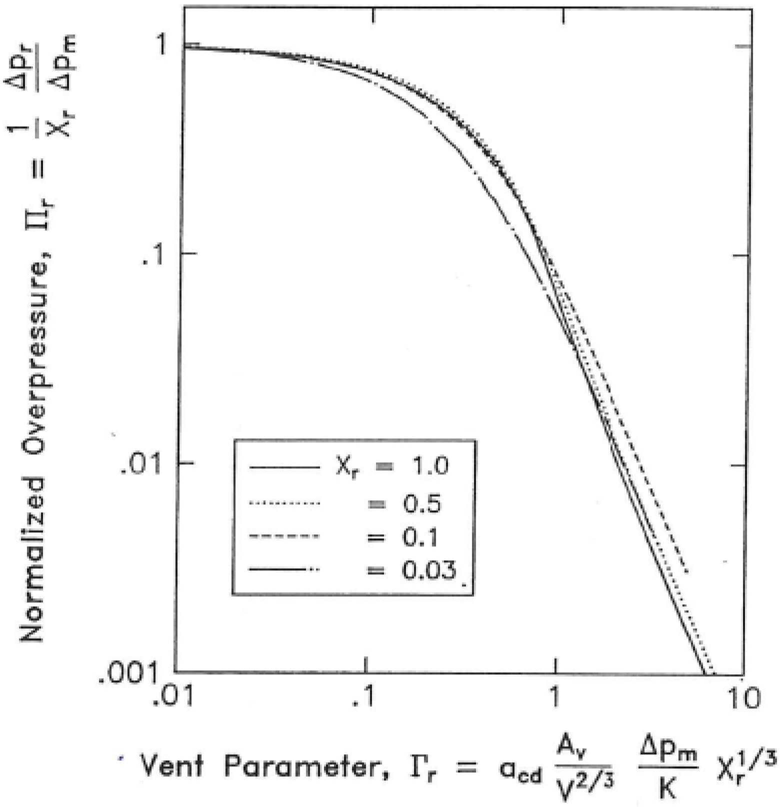

Figure 10. Partial volume model predictions using the modified vent and overpressure parameters (Tamanini 1996).

The dependence of A_v on filled fraction X_r and, consequently, on the parameter m is quite weak. The partial volume explosion model itself, however, has limitations some of which are directly inherited from the full volume explosion model and others arise from the additional assumptions made to adapt the full volume model to the case of partial volume explosions.

— An obvious problem arises from the implicit assumption that the flammable portion is geometrically similar to the enclosure volume and is in some central location, neither too close nor too far from the vent.

— Another possible source of uncertainty is associated with the arbitrary selection of the criterion to slow down the flame as it approaches the boundary of the flammable mixture. This choice is probably inadequate if geometric similarity is not maintained, as the flammable volume is reduced relative to the enclosure. This would be the case of partial volume explosions involving stratified mixtures.

6.3 The model of Orlando

Dahoe et al. (1996) present a simple model for gas or dust explosion in a closed spherical vessel. Unburned and burned gases are assumed to be ideal gases with equal and constant specific heats. A discontinuous transition from unburned to burned gas takes place at the infinitely thin spherical flame front which propagates at a constant velocity. The compression of the unburned mixture is adiabatic.

The mass fraction of the unburned mixture in the vessel is related to the fractional pressure rise by the approximate equation originally presented by Lewis and von Elbe (1987)

$$\frac{m_u}{m_0} = \frac{p_m - p}{p_m - p_0} \tag{89}$$

Differentiation of Eq. (89) with respect to time yields

$$\frac{dp}{dt} = -\frac{p_m - p_0}{m_0}\frac{dm_u}{dt} \tag{90}$$

where the mass consumption rate of unburned mixture dm_u/dt can be expressed as

$$\frac{dm_u}{dt} = -A_f S_0 \rho_u \tag{91}$$

For adiabatic compression of the unburned mixture

$$\rho_u = \rho_0 \left(\frac{p}{p_0}\right)^{1/\gamma} \tag{92}$$

Inserting Eqs. (91) and (92) into Eq. (90) the pressure rise rate becomes

$$\frac{dp}{dt} = \frac{A_f S_0 \rho_0}{m_0}(p_m - p_0)\left(\frac{p}{p_0}\right)^{1/\gamma} \tag{93}$$

The original derivation by Dahoe et al. (1996) is for a vessel filled with homogenous gas mixture and the ratio $m_0/\rho_0 = V$. For a vessel of whose volume only the part V/m is filled with such a mixture, $m_0/\rho_0 = V/m$.

From Eq. (93) it can be seen that dp/dt increases monotonically with p and hence the maximum rate of pressure rise is attained when $p = p_m$. By substituting $p = p_m$ into Eq. (93), the following expression is found the maximum value of dp/dt

$$\left(\frac{dp}{dt}\right)_{\max} = \frac{mS_0 A_f}{V}(p_m - p_0)\left(\frac{p_m}{p_0}\right)^{1/\gamma} \tag{94}$$

For a partial volume explosion, p_m in Eq. (94) has to be evaluated as a function of m. Making the same assumptions as Dahoe et al. (1996), Orlando (2004) derives a simple estimate of p_m as a function of the filling degree m from the conservation of internal energy

$$p_m = \left(1 - \frac{1}{m} + \frac{s}{m}\frac{T_{ad}}{T_0}\right)p_0 \tag{95}$$

where T_0 is the initial temperature [K], T_{ad} is the adiabatic flame temperature reached in the vessel for a homogenous stoichiometric mixture, and s is the ratio N_f/N_i when the combustion reactions have been described with a global single step mechanism.

Substituting Eq. (95) into Eq. (94), one finds

$$\left(\frac{dp}{dt}\right)_{\max} = \frac{mS_0 A_f}{V} p_0 \left(\frac{s}{m}\frac{T_{ad}}{T_0} - \frac{1}{m}\right)\left(1 + \frac{s}{m}\frac{T_{ad}}{T_0} - \frac{1}{m}\right)^{1/\gamma} \tag{96}$$

Introducing the expansion factor $E = sT_{ad}/T_0$ into Eq. (96)

$$\left(\frac{dp}{dt}\right)_{\max} = \frac{mS_0 A_f}{V} p_0 \left(\frac{E-1}{m}\right)\left(1 + \frac{E-1}{m}\right)^{1/\gamma} \tag{97}$$

A rough estimate of the flame area A_f is obtained assuming that the flame has propagated in a flammable layer of height h inside a vessel of side L and height H.

Based on the test results of Liebman et al. (1970) and Feng et al. (1975), the flame front height can be conservatively assumed to be 3h for m > 1 (h < H). With these assumptions, the ratio of A_f/V for central ignition is

$$\frac{A_f}{V} = \frac{4L \times 3h}{L^2 H} = \frac{12h}{LH} = \frac{12}{mL} \tag{98}$$

Similarly, for end ignition $A_f/V = 6/mL$.

By inserting Eq. (98) into Eq. (97) and the resulting expression into Eq. (15), the following expression called "stratified cubic root law" is obtained for K_G

$$K_G(m) = k\frac{S_0}{L}p_0\left(\frac{E-1}{m}\right)\left(1+\frac{E-1}{m}\right)^{1/\gamma}V^{1/3} \tag{99}$$

where k = 12 for central ignition and k = 6 for end ignition (Orlando et al. 2007).

The limits of validity of Eq. (99) have to be estimated experimentally. However, to the knowledge of Orlando et al. (2007), the only experimental data on K_G as a function of the filling degree m comes from the tests by Tamanini (2000) in a very limited range of experimental conditions. Therefore Orlando (2004) developed a detailed two-dimensional combustion model to simulate the laminar flame propagation in layered homogenous, stoichiometric methane-air mixtures with different values of the filling degree m, within an elongated closed vessel, with both central and lateral ignition. This model was implemented in a commercial CFD program.

Experimental conditions used for validation and subsequent computations were very similar to those by Feng et al. (1975). The height h of the homogenous flammable layer of methane was kept constant at 42 mm, and the total height of the gallery H was varied with the filling degree m. Simulations were done with the values 1, 2, 5, 11 and 22 of m. The values of K_G(m) obtained from the simulations strongly decreased with m and the values of $(dp/dt)_{max}$ increased as the volume V decreased. That is, the stratified cubic root law Eq. (99) was approximately verified. In the case of end ignition, lower values of K_G(m) were obtained than for central ignition, while trends with m and V were confirmed.

The values of K_G(m) obtained from the simulation were compared to those calculated with Eq. (99) and found to be very similar. This shows that Eq. (99) correctly described the functional dependence on the filling ratio parameter m. For all cases, this functional dependence was very close to the simple equation

$$K_G(m) = \frac{K_G}{m} \tag{100}$$

where K_G refers to full volume deflagration in a spherical vessel. Values obtained with Eq. (100) resulted to be always greater than all the other values obtained from Eq. (99) or CFD simulations, apart from the case m = 1. Moreover, Eq. (100) is conservative for m > 1 and returns to the value of K_G for full volume deflagration when m = 1.

The correlation Eq. (100) is general: stratified mixtures are considered to have lower reactivity than full volume, well-mixed stoichiometric flammable mixtures. Uncertainties in the amount of fuel forming the flammable mixture are avoided by assuming that all the released fuel forms a stoichiometric layer. Hence Bartknecht correlation for vent dimensioning of high-strength enclosures Eq. (16) can be applied simply by adopting the new value of K_G(m).

For low-strength enclosures, the modified Runes' formula Eq. (13) in the 2002 edition of NFPA 68 cannot be used for partial volume explosions since it is independent of fuel reactivity. (However, in the 2007 edition of NFPA 68 it is dependent on fuel reactivity through Eq. (14).) That is why Orlando et al. (2007) use Bradley and Mitcheson model for low-strength enclosures. The only modification needed is to redefine the dimensionless burning velocity as

$$\bar{S}_0 = \frac{S_0}{mc}(E-1) \qquad (101)$$

Solving $P_{stat} = P_{red}$ from Eq. (64)

$$P_{red} \le 12.46\left(\frac{\bar{A}}{\bar{S}_0}\right)^{-2} \qquad (102)$$

For the case of $P_{red} > P_{stat}$, solving P_{red} from Eq. (66) gives

$$P_{red} \le 4.85 P_{stat}^{0.375}\left(\frac{\bar{A}}{\bar{S}_0}\right)^{-1.25} \qquad (103)$$

The overpressures P_{stat} and P_{red} in Eqs. (102) and (103) are given in bar. Obviously, when $P_{red} = P_{stat}$ Eq. (103) reduces into Eq. (102) (Orlando et al. 2007).

6.4 Validation of the model of Orlando

Orlando et al. (2007) use test data by Buckland (1980), Palmer and Tonkin (1980), Bartknecht (1981), Tamanini (2000) and DeHaan et al. (2001) to validate Eqs. (100) to (103). They calculate with the methods of Tamanini and their own the predicted vent areas A_v corresponding to the measured values of P_{red}.

In this report, the calculations using Eqs. (100) to (103) have been repeated. Some minor errors detected in Tables 3 and 4 of (Orlando et al. 2007) have been rectified. Instead of the value of the discharge coefficient used by these authors C_d = 0.9 (Marra 2009), the original value of 0.6 used by Bradley and Mitcheson (1978b) has been inserted into Eq. (57). The values predicted with the method of Tamanini have been taken from Tables 3 and 4 of (Orlando et al. 2007). The resulting values of A_v are presented in Tables 2 to 14 below, where also the ratios of predicted to actual values of A_v have been given.

Buckland (1980) performed tests in a 27.2 m^3 chamber 3.66 m long, 3.05 m wide and 2.44 m high, provided with a full width opening, extending downwards from the roof, in one of the 3.05 m × 2.44 m walls. Three vent sizes were used: A_v = 0.93, 1.86 and 3.72 m^2. The vent was covered by a 0.05 mm or 0.13 mm thick polyethylene film. Tests were also performed with glass windows, with a total area of 1.5, 2.25 or 3.0 m^2, as vent cover. When glass windows were used as vent covers, the three glass panes broke one by one at different overpressures. The vent area increased stepwise and the vent was not fully open until all the panes had broken.

Natural gas containing between 92 and 94 % of methane was used as fuel. Gas-air mixture of 10 % concentration was introduced at roof level at a rate that allowed a layer to be formed, displacing air through valves situated 0.5 m above the bottom of the chamber. Layering was accomplished by use of four 0.9 m diameter diffusers. Samples of the gas mixture were remotely drawn from the chamber and analysed automatically by gas chromatography.

The nominal layer depth was defined as the lowest depth from roof to which 95 % of the desired methane concentration existed. The concentration 0.305 m below the nominal layer depth ranged 5.5 to 7.5 % i.e. it was somewhat larger than the LFL of methane 5.0 %. The concentration 0.61 m below the nominal layer depth ranged 1.1 to 1.7 %.

The flammable layer was ignited by a number of different ignition sources and the resulting pressure pulses were measured both inside and outside the chamber. The values and times of two pressure peaks (i.e. P_1 and P_2) were recorded for six layer depths corresponding to m = 1.33, 1.40, 1.59, 2.00 and 2.63. The predicted values of A_v were calculated assuming $P_{stat} = P_1$ and $P_{red} = P_2$ with S_0 = 0.43 m/s, E = 7.52 (Bradley & Mitcheson 1978b), K_G = 55 bar·m/s (Bartknecht 1993) and A_s = 55 m^2.

Table 2. Tests by Buckland (1980), A_v = 0.93 m^2 and m = 2.0

P_{stat}	P_{red}	Tamanini	Tam./A_v	Orlando	Orl./A_v
0.007	0.083	0.5	0.55	2.2	2.4
0.010	0.076	0.5	0.55	2.7	2.9
0.007	0.055	0.6	0.65	3.1	3.3
0.007	0.035	0.8	0.85	4.4	4.7
0.007	0.025	1.0	1.1	5.8	6.2
0.004	0.055	0.6	0.65	2.6	2.8

Table 3. Tests by Buckland (1980), A_v = 3.72 m^2 and m = 1.59

P_{stat}	P_{red}	Tamanini	Tam./A_v	Orlando	Orl./A_v
0.017	0.066	0.6	0.15	4.3	1.2
0.066	0.06	0.6	0.15	6.7	1.8
0.057	0.109	0.5	0.15	4.5	1.2
0.063	0.050	0.7	0.20	7.5	2.0

Table 4. Tests by Buckland (1980), A_v = 1.86 m^2 and m = 2.63

P_{stat}	P_{red}	Tamanini	Tam./A_v	Orlando	Orl./A_v
0.017	0.098	0.5	0.25	1.9	1.0
0.090	0.066	0.6	0.30	4.0	2.2
0.070	0.070	0.5	0.25	3.8	2.0
0.075	0.130	0.4	0.20	3.3	1.8

Table 5. Tests by Buckland (1980), A_v = 1.86 m^2 and m = 2.0

P_{stat}	P_{red}	Tamanini	Tam./A_v	Orlando	Orl./A_v
0.019	0.109	0.5	0.25	4.2	2.3
0.076	0.101	0.5	0.25	4.3	2.3
0.079	0.102	0.5	0.25	4.3	2.3

Table 6. Tests by Buckland (1980), m = 1.33

P_{stat}	A_v	P_{red}	Tamanini	Tam./A_v	Orlando	Orl./A_v
0.072	3.0	0.110	0.5	0.15	4.8	1.6
0.039	2.25	0.110	0.5	0.20	4.8	2.1
0.115	1.5	0.219	0.3	0.20	3.3	2.2
0.086	1.5	0.221	0.3	0.20	3.2	2.1

Palmer and Tonkin (1980) performed tests with a 10 % methane-air mixture in the same chamber as Buckland (1980). Several values of layer depth were used corresponding to values of m from 1.3 to 8.1. The gas layer was ignited by a single high-voltage spark at the interface between the gas layer and the underlying air. The igniter was located centrally within the chamber adjacent the wall remote from the vent.

Polyethylene film, glass windows and hinged doors were used as vent covers. When a lightweight vent cover was used, an approximately constant bursting pressure, about 0.6 kPa, was obtained with different vent areas by using a polyethylene sheet 0.05 mm thick. Glass windows were mounted in a wooden frame which was attached directly to the vent. The frame contained three glass panes, 3 or 5 mm thick. The door vent cover had an area of 1.86 m^2 and was hinged on the lower side.

Table 7. Tests by Palmer & Tonkin (1980), A_v = 3.72 m^2 and P_{stat} = 0.006 bar

m	P_{red}	Tamanini	Tam./A_v	Orlando	Orl./A_v
2.71	0.008	1.7	0.45	6.9	1.9
1.95	0.015	1.3	0.35	5.8	1.6
1.63	0.020	1.2	0.30	6.5	1.5
1.28	0.030	1.0	0.25	6.0	1.3

Table 8. Tests by Palmer & Tonkin (1980), A_v = 1.86 m^2 and P_{stat} = 0.006 bar

m	P_{red}	Tamanini	Tam./A_v	Orlando	Orl./A_v
4.06	0.020	1.0	0.25	2.2	1.2
1.88	0.040	0.8	0.20	2.7	1.5
1.32	0.065	0.7	0.20	2.6	1.4

Table 9. Tests by Palmer & Tonkin (1980), A_v = 0.93 m^2 and P_{stat} = 0.006 bar

m	P_{red}	Tamanini	Tam./A_v	Orlando	Orl./A_v
8.13	0.039	0.6	0.65	0.65	0.7
8.13	0.044	0.6	0.65	0.60	0.6
4.88	0.042	0.7	0.75	1.0	1.1
4.06	0.057	0.6	0.65	0.95	1.0
2.71	0.062	0.6	0.65	1.3	1.4
1.95	0.075	0.6	0.65	1.6	1.7
1.74	0.073	0.6	0.65	1.8	2.0
1.63	0.095	0.5	0.55	1.6	1.7
1.43	0.087	0.6	0.65	1.9	2.1
1.32	0.120	0.5	0.55	1.6	1.7

Table 10. Tests by Palmer & Tonkin (1980), m = 1.33

A_v	P_{stat}	P_{red}	Tamanini	Tam./A_v	Orlando	Orl./A_v
1.5	0.040	0.269	0.3	0.2	2.9	1.9
3.0	0.072	0.110	0.5	0.17	4.8	1.6
1.5	0.115	0.193	0.4	0.27	3.5	2.4

Table 11. Tests by Palmer & Tonkin (1980), A_v = 1.86 m²

m	P_{stat}	P_{red}	Tamanini	Tam./A_v	Orlando	Orl./A_v
1.33	0.119	0.161	0.4	0.22	3.95	2.1
2.0	0.051	0.114	0.5	0.27	4.0	2.2
2.0	0.081	0.091	0.5	0.27	4.3	2.3

Bartknecht (1981, 1993) performed tests in a 60 m^3 concrete bunker partially (10 m^3) filled with propane-air mixture. The dimensions of the bunker are not given. The mixture was ignited with a spark gap. The vent area A_v ranged from 1 to 4 m^2. Vent opening pressure P_{stat} was 0.1 bar, 0.2 bar or 0.5 bar. No further information about the tests is given. The predicted values of A_v were calculated assuming K_G = 100 bar·m/s (Bartknecht 1993).

Table 12. Tests by Bartknecht (1981), m = 6

A_v	P_{stat}	P_{red}	Tamanini	Tam./A_v	Orlando	Orl./A_v
4	0.1	0.30	0.70	0.18	3.00	0.75
4	0.2	0.35	0.64	0.16	3.25	0.81
2	0.1	0.45	0.57	0.29	2.40	1.20
3	0.1	0.35	0.64	0.21	2.75	0.92
2	0.5	0.70	0.44	0.22	3.15	1.58
3	0.5	0.55	0.51	0.17	3.65	1.21

Tamanini (2000) used a 63.7 m^3 chamber with a square basis 4.57 m on the side and 3.05 m high. The surface area A_s was 97 m^2. It was fitted with openings on the roof for explosion venting each 0.57 m^2. The flammable layers were formed through controlled injection of propane near the floor of the chamber using nine circular diffusers 0.3 m in diameter. This injection method was believed to provide a reasonable simulation of the dynamics of vapour diffusion process, as it would occur above a vaporising pool of flammable liquid.

The range of conditions covered by the tests involved injected amounts of propane that would have filled from 4 to 29 % on the enclosure volume with a stoichiometric mixture. The structure of the mixture layer was quantified with two parameters: the premixed and rich (i.e. with a concentration larger than UFL) filled fractions: X_p and X_r. The premixed fraction ratio $X_p/(X_p + X_r)$ represents the fraction of the total reactive mixture that supports premixed flame propagation as opposed to diffusive or convective burning.

The mean of flame speed values in eight tests with no obstacles in the enclosure was 1.75±0.25 m/s, essentially independent of the premixed fraction ratio. Since the laminar burning velocity of stoichiometric propane is 0.41 m/s, these flame speed values were 4.3±0.6 times S_0, which supports the general consensus that the flame speed in layered mixtures is of the order of 3 to 5 times S_0.

The purpose of the tests was the validation of a computer model for enclosures partially filled with vapours from spills of flammable liquids. The overpressure data has been published of only one of the 33 tests. In this particular test, one vent was covered with 0.10 mm polyethylene sheet while the others were closed. This was one of the tests where a checkerboard arrangement of square obstacle plates 0.15 m above the floor was used to provide a 50 % blockage to vertical expansion. The values of the first and second pressure peaks were 0.031 and 0.033 bar, respectively (Tamanini 2000). Data on the values of P_{stat} in Table 13 is taken from Orlando (2004). All the other data items are taken from Orlando et al. (2007). All tests in Table 13 except the first one were performed with the obstacle plates (Orlando 2004). The predicted values of A_v were calculated assuming $S_0 = 0.45$ m/s and E = 7.98 (Bradley & Mitcheson 1978b).

Table 13. Tests by Tamanini (2000)

m	A_v	P_{stat}	P_{red}	Tamanini	Tam./A_v	Orlando	Orl./A_v
15	1.14	0.028	0.019	2.49	2.20	1.75	1.5
15	0.57	0.032	0.072	1.27	2.25	0.85	1.5
8.2	0.57	0.032	0.033	2.06	3.60	2.85	5.0
5.0	0.57	0.031	0.033	2.24	3.95	4.65	8.1
4.2	0.57	0.023	0.030	2.41	4.25	5.40	9.4

DeHaan et al. (2001) performed tests in a 20 m^3 explosion chamber, using laboratory grade hexane as the fuel vapour source. The chamber was 3.6 m long, 2.4 m wide and 2.4 m high, with a 1.0 m^2 square vent opening in the front wall. The surface area A_s was 46.1 m^2. The vent opening was covered with a 1.2 m × 1.2 m sheet of 12.5 mm thick fibreboard which acted as the pressure relief.

A 1.0 m^2 square steel tray was located on a load cell on the floor near the centre of the chamber. The tray was filled with a layer of water at 20 °C 80 mm deep. Hexane was poured to the tray so that it formed a continuous layer 2 mm deep on top of the water, leaving a rim less than 20 mm projecting above the surface. Once the hexane was added, the chamber was sealed. A uniform layer was expected to be formed at floor level, as the hexane was allowed to evaporate.

Once the required mass of hexane had evaporated, the mixture was ignited using capacitive spark igniters located on the wall furthest from the vent. In total five tests were performed with the mass of hexane evaporated ranging from 130 to 158 g and ignited either 0.05 or 0.20 m above the floor. The pressure/time profiles consisted of one pressure peak, followed by pressure oscillations with decreasing amplitudes. The average overpressure of this peak was 5.9 kPa, which was the failure pressure of the 12.5 mm thick fibreboard panel. The predicted values of A_v in Table 14 have been calculated assuming that hexane vapour has the same values of S_0 and E as propane above.

Table 14. Tests by DeHaan (2001), $A_v = 1.0$ m^2, $P_{stat} = P_{red}$

m	P_{red}	Tamanini	Tam./A_v	Orlando	Orl./A_v
11.8	0.052	0.5	0.5	0.95	0.95
11.55	0.059	0.4	0.4	0.90	0.90
12.35	0.047	0.5	0.5	0.95	0.95
10.15	0.047	0.5	0.5	0.95	0.95
10.50	0.058	0.5	0.5	1.00	1.00

The values of the ratio of vent areas predicted by Eqs. (101) to (103) for tests where $P_{red} < 10$ kPa and actual vent areas are plotted in Figure 11. The corresponding ratios predicted by Eqs. (16) and (100) for tests where $P_{red} > 10$ kPa and actual vent areas are plotted in Figure 12.

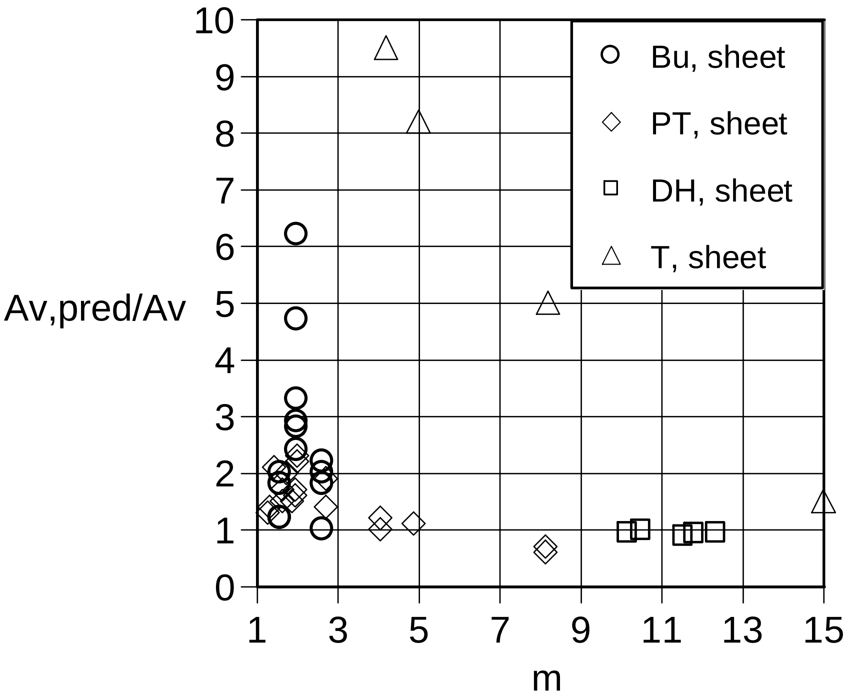

Figure 11. The ratios of vent areas predicted by Eqs. (101) to (103) to the actual vent areas for tests where P_{red} < 10 kPa.

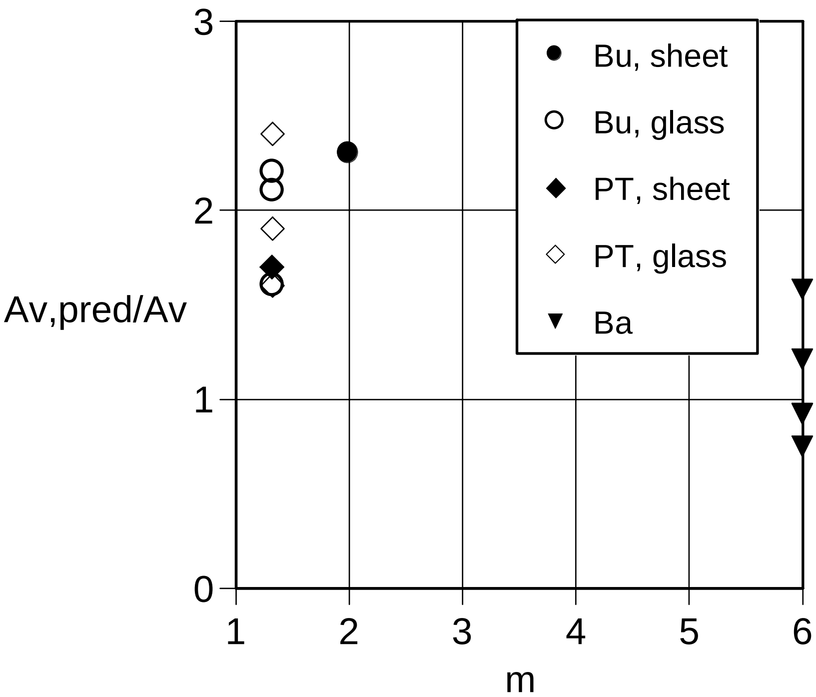

Figure 12. The ratios of vent areas predicted by Eqs. (16) and (100) to the actual vent areas for tests where P_{red} > 10 kPa.

It is seen from Fig. 11 that the method of Orlando et al. (2007) gives conservative predictions (i.e. overestimates the required vent area) for almost all the tests. For just two of the 40 tests the method is non-conservative i.e. underestimates the vent area.

With a few exceptions, the ratio $A_{v,pred}/A_v$ ranges from 1 to 3. The tests by Tamanini with 4.2 ≤ m ≤ 8.2 are a notable exception. These tests were performed with the configuration of obstacle plates above the floor of the enclosure. Probably the mixing caused by obstacle plates prevented the stratification of the propane-air layer. If this is true, the flame propagated in a more or less homogenous layer with approximately laminar flame speed. Consequently, the overpressures were lower than in the tests with no obstacles and a stratified layer. Also two tests by Buckland with $A_v = 0.93\ m^2$ and m = 2 have a value of this ratio larger than about 3.

Similar conclusions can be drawn from Fig. 12 concerning those tests with P_{red} > 10 kPa. With a few exceptions, the ratio $A_{v,pred}/A_v$ ranges from 1 to 2.4. For three tests by Bartknecht the method gives non-conservative predictions with $A_{v,pred}/A_v \geq 0.75$.

On the contrary, the method of Tamanini gives mostly non-conservative predictions as can be seen from Tables 2 to 14. The ratio $A_{v,pred}/A_v$ can be as low as 0.15.

6.5 Use of the method of Molkov for partial volume explosions

Since the method of Molkov et al. (2000) is an improvement to the method of Bradley and Mitcheson (1978a), and is dependent on the reactivity of the flammable mixture through the Bradley number Eq. (67) or (68), it is worth while to apply the simple idea by Orlando et al. (2007) of dividing the burning velocity S_0 by the parameter m also to this model. In other words, the Bradley number Br is multiplied by m for partial volume explosions.

However, since the vent area A_v corresponding to a given value of the reduced overpressure P_{red} cannot be solved from Eqs. (67) to (72), a validation of the method comparing the predicted and actual values of A_v is not feasible. Instead, the validation is done in the opposite way: the predicted values of P_{red} corresponding to the actual values of A_v are calculated and compared to the measured values of P_{red}. The values of P_{red} are predicted both with the method of Orlando et al. (2007), where the value of burning velocity is set equal to S_0/m or the value of the deflagration index is set equal to K_G/m, and the method of Molkov et al. (2000), where the value of burning velocity is set equal to S_0/m.

The ratio of the dimensionless numbers $\bar{S}_0$ Eq. (101) and $\bar{A}$ Eq. (57) is

$$\frac{\bar{S}_0}{\bar{A}} = \frac{S_0(E-1)A_s}{cC_d m A_v} \tag{104}$$

The ratio $\bar{S}_0/\bar{A}$ is inserted either in Eq. (102) or Eq. (103) depending on the values of P_{stat} and P_{red} found by iteration. The value of P_{red} can be solved from the Bartknecht equation Eq. (16) only when P_{stat} = 0.1 bar

$$P_{red} = \left(\frac{0.1265\log(K_G/m) - 0.0567}{A_v} V^{2/3} \right)^{1/0.5817} \tag{105}$$

When P_{stat} > 0.1 bar, the latter term is dependent on P_{red}

$$P_{red} = \left(\frac{0.1265\log(K_G/m) - 0.0567 + 0.1754(P_{stat} - 0.1)P_{red}^{0.0095}}{A_v} V^{2/3} \right)^{1/0.5817} \tag{106}$$

The term $P_{red}^{0.0095}$, however, is almost equal to unity and can be neglected.

Results of the exercise are presented in Tables 15 to 27.

Table 15. Tests by Buckland (1980), A_v = 0.93 m^2 and m = 2.0

P_{stat}	P_{red}	Orlando	Orl./P_{red}	Molkov	Mol./P_{red}
0.007	0.083	0.245	3.0	0.46	5.5
0.010	0.076	0.280	3.7	0.46	6.1
0.007	0.055	0.245	4.5	0.46	8.4
0.007	0.035	0.245	7.0	0.46	13.1
0.007	0.025	0.245	9.8	0.46	18.4
0.004	0.055	0.198	5.7	0.46	8.4

Table 16. Tests by Buckland (1980), A_v = 3.72 m^2 and m = 1.59

P_{stat}	P_{red}	Orlando	Orl./P_{red}	Molkov	Mol./P_{red}
0.017	0.066	0.080	1.2	0.044	0.7
0.066	0.060	0.204	3.3	0.043	0.7
0.057	0.109	0.127	1.15	0.043	0.4
0.063	0.050	0.204	4.1	0.042	0.2

Table 17. Tests by Buckland (1980), A_v = 1.86 m^2 and m = 2.63

P_{stat}	P_{red}	Orlando	Orl./P_{red}	Molkov	Mol./P_{red}
0.017	0.098	0.102	1.05	0.064	0.65
0.090	0.066	0.298	4.5	0.062	0.95
0.070	0.070	0.298	4.2	0.062	0.9
0.075	0.130	0.178	1.4	0.062	0.5

Table 18. Tests by Buckland (1980), A_v = 1.86 m^2 and m = 2.0

P_{stat}	P_{red}	Orlando	Orl./P_{red}	Molkov	Mol./P_{red}
0.019	0.109	0.15	1.4	0.111	1.0
0.076	0.101	0.25	2.5	0.108	1.05
0.079	0.102	0.25	2.5	0.108	1.05

Table 19. Tests by Buckland (1980), m = 1.33

P_{stat}	A_v	P_{red}	Orlando	Orl./P_{red}	Molkov	Mol./P_{red}
0.072	3.0	0.110	0.23	2.05	0.094	0.85
0.039	2.25	0.110	0.26	2.35	0.171	1.55
0.115	1.5	0.219	0.64	2.9	0.38	1.75
0.086	1.5	0.221	0.58	2.6	0.38	1.7

Table 20. Tests by Palmer & Tonkin (1980), A_v = 3.72 m^2 and P_{stat} = 0.006 bar

m	P_{red}	Orlando	Orl./P_{red}	Molkov	Mol./P_{red}
2.71	0.008	0.028	3.5	0.015	1.9
1.95	0.015	0.042	2.8	0.029	1.95
1.63	0.020	0.053	2.6	0.042	2.1
1.28	0.030	0.071	2.4	0.068	2.25

Table 21. Tests by Palmer & Tonkin (1980), A_v = 1.86 m^2 and P_{stat} = 0.006 bar

m	P_{red}	Orlando	Orl./P_{red}	Molkov	Mol./P_{red}
4.06	0.020	0.040	2.0	0.027	1.35
1.88	0.040	0.105	2.6	0.13	3.2
1.32	0.065	0.165	2.5	0.26	4.0

Table 22. Tests by Palmer & Tonkin (1980), A_v = 0.93 m^2 and P_{stat} = 0.006 bar

m	P_{red}	Orlando	Orl./P_{red}	Molkov	Mol./P_{red}
8.13	0.039	0.040	1.0	0.027	0.7
8.13	0.044	0.040	0.9	0.027	0.6
4.88	0.042	0.076	1.8	0.075	1.8
4.06	0.057	0.095	1.65	0.108	1.9
2.71	0.062	0.158	2.55	0.245	4.0
1.95	0.075	0.239	3.2	0.485	6.5
1.74	0.073	0.275	3.75	0.615	8.5
1.63	0.095	0.299	3.15	0.705	7.4
1.43	0.087	0.352	4.05	0.925	10.6
1.32	0.120	0.389	3.25	1.10	9.1

Table 23. Tests by Palmer & Tonkin (1980), m = 1.33

A_v	P_{stat}	P_{red}	Orlando	Orl./P_{red}	Molkov	Mol./P_{red}
1.5	0.040	0.269	0.43	1.6	0.39	1.45
3.0	0.072	0.110	0.23	2.1	0.094	0.85
1.5	0.115	0.193	0.64	3.3	0.38	1.95

Table 24. Tests by Palmer & Tonkin (1980), A_v = 1.86 m^2

m	P_{stat}	P_{red}	Orlando	Orl./P_{red}	Molkov	Mol./P_{red}
1.33	0.119	0.161	0.50	3.1	0.24	1.5
2.0	0.051	0.114	0.22	1.9	0.11	0.95
2.0	0.081	0.091	0.26	2.9	0.11	1.2

Table 25. Tests by Bartknecht (1981), m = 6

A_v	P_{stat}	P_{red}	Orlando	Orl./P_{red}	Molkov	Mol./P_{red}
4	0.1	0.30	0.185	0.6	0.011	0.04
4	0.2	0.35	0.245	0.7	0.011	0.03
2	0.1	0.45	0.61	1.35	0.046	0.10
3	0.1	0.35	0.305	0.85	0.020	0.07
2	0.5	0.70	1.55	2.2	0.046	0.07
3	0.5	0.55	0.84	1.5	0.020	0.04

Table 26. Tests by Tamanini (2000)

m	A_v	P_{stat}	P_{red}	Orlando	Orl./P_{red}	Molkov	Mol./P_{red}
15	1.14	0.005	0.019	0.032	1.7	0.026	1.35
15	0.57	0.016	0.072	0.115	1.6	0.101	1.4
8.2	0.57	0.016	0.033	0.245	7.5	0.353	10.7
5.0	0.57	0.016	0.033	0.460	14	0.97	29.5
4.2	0.57	0.011	0.030	0.495	16.5	1.41	47

Table 27. Tests by DeHaan (2001), A_v = 1.0 m^2, P_{stat} = P_{red}

m	P_{red}	Tamanini	Tam./A_v	Molkov	Mol./P_{red}
11.8	0.052	0.045	0.85	0.008	0.16
11.55	0.059	0.047	0.80	0.009	0.15
12.35	0.047	0.041	0.90	0.008	0.17
10.15	0.047	0.061	1.3	0.011	0.24
10.50	0.058	0.057	1.0	0.011	0.18

When the three last tests in Table 15 and the three last tests in Table 26 are neglected, the ratio of the values of P_{red} predicted by the method of Orlando and experimental values ranges from 0.6 to 4.5. The predictions are generally conservative, only in six tests of 60 the reduced pressure is somewhat underpredicted. The method by Molkov gives a larger variation of the ratio: from 0.03 to 10.6. The experimental overpressure is underpredicted altogether for 24 tests, sometimes significantly.

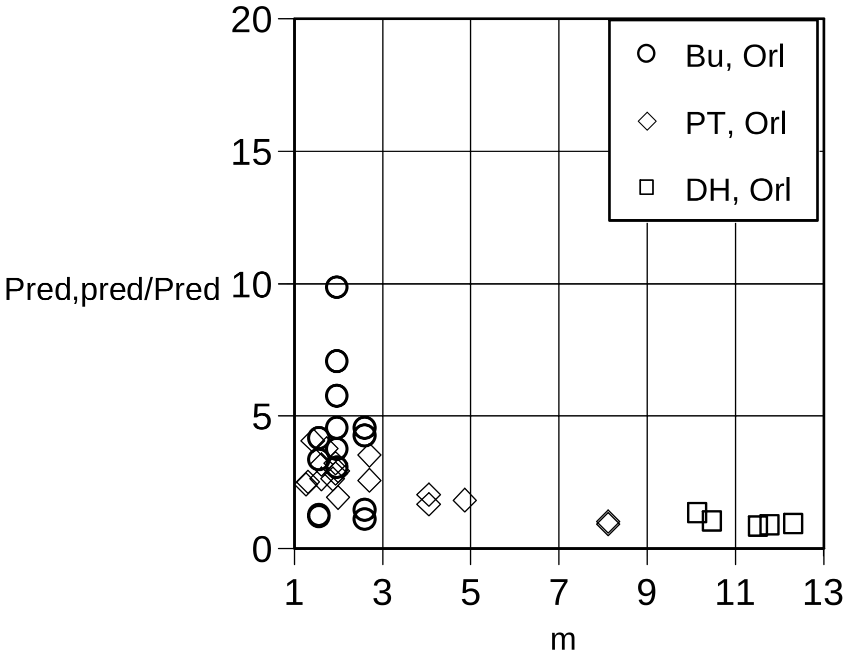

Figure 13. The ratios of reduced overpressures P_{red} predicted by the method of Orlando Eqs. (101) to (103) to the measured overpressures for tests where P_{red} < 10 kPa.

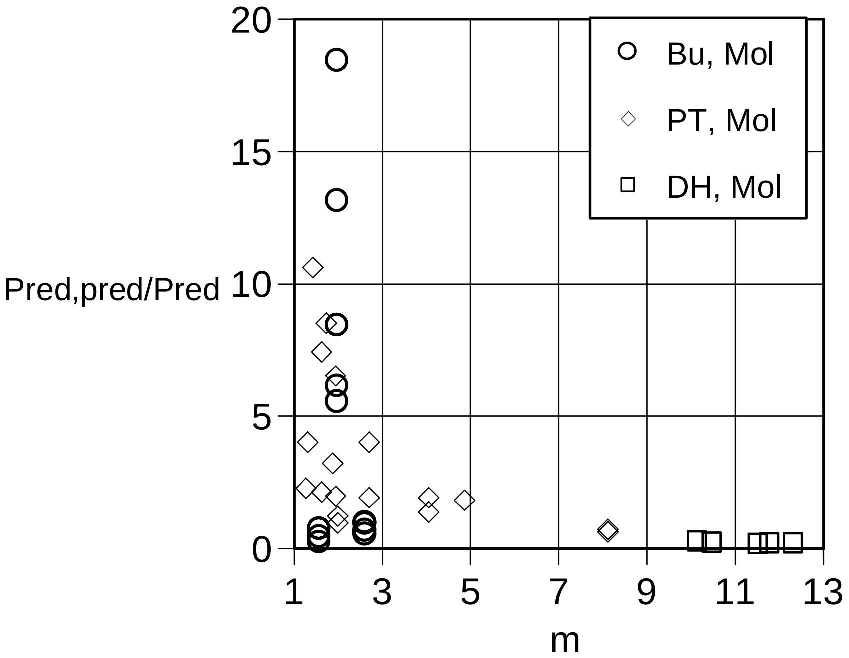

Figure 14. The ratios of reduced overpressures P_{red} predicted by the method of Molkov Eqs. (67) to (72) to the measured overpressures for tests where P_{red} < 10 kPa.

The ability of the methods of Orlando and Molkov to predict the measured values of the reduced pressure P_{red} can be seen from Figs. 13 and 14 for P_{red} < 0.1 bar and Figs. 15 and 16 for P_{red} > 0.1 bar. To facilitate comparison of the two methods, one test in Table 22 with $P_{red,pred}/P_{red}$ = 9.1 has been omitted from Fig. 16.

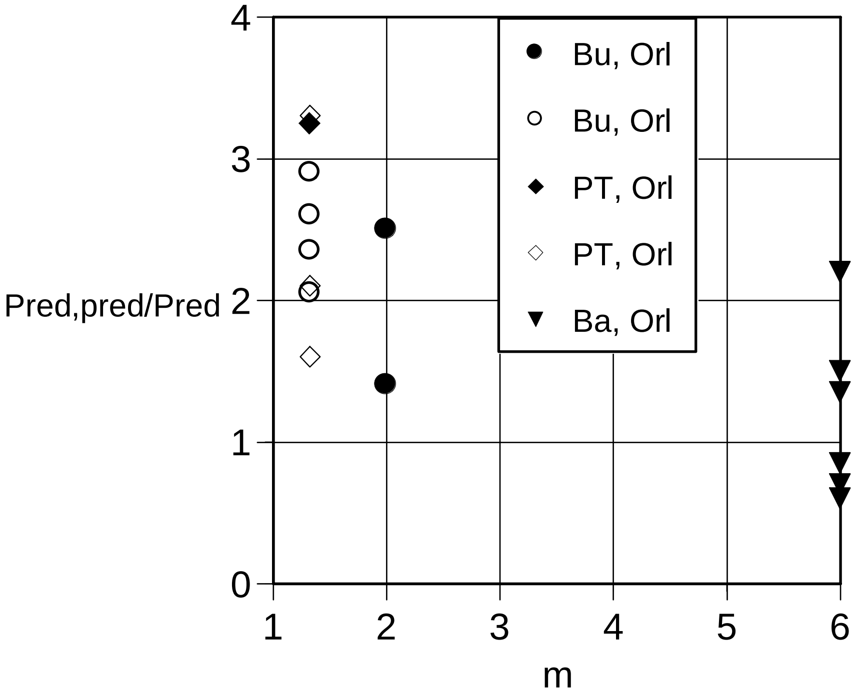

Figure 15. The ratios of reduced overpressures P_{red} predicted by the method of Orlando Eqs. (101) to (103) to the measured overpressures for tests where P_{red} > 10 kPa.

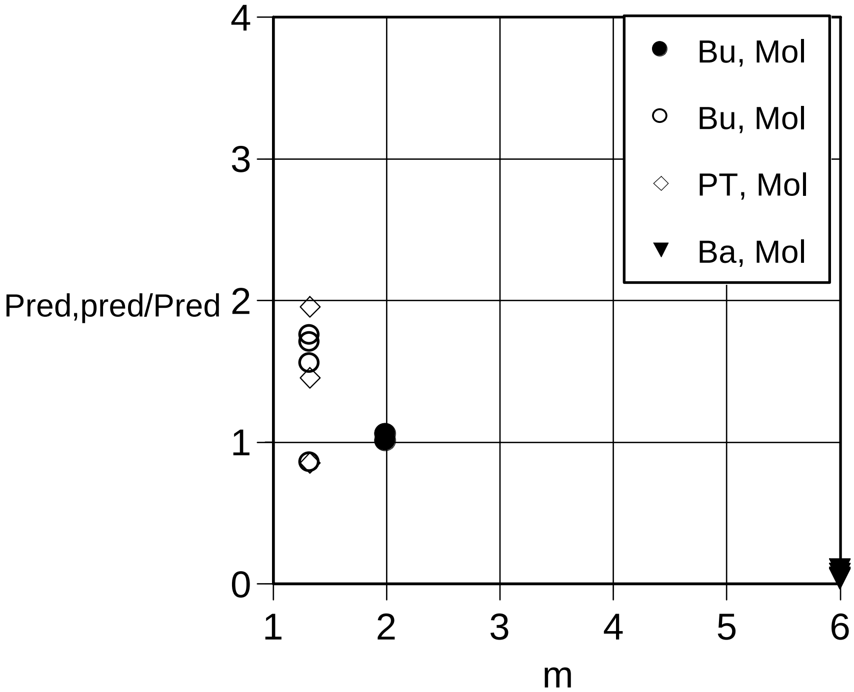

Figure 16. The ratios of reduced overpressures P_{red} predicted by the method of Molkov Eqs. (67) to (72) to the measured overpressures for tests where P_{red} > 10 kPa.

6.6 Other types of partial volume explosions

Naidus (1981) performed tests in a 27.2 m^3 room whose walls and ceiling were of plywood fixed on a light steel frame. The wall area A_s was 55.0 m^2. Provision was made for mounting an explosion relief vent on a wall 3.66 m wide and 2.44 m high. The vent area was either 1.5, 2.25 or 3.0 m^2. Vent covers were standard models. Explosion tests were also run with uncovered vents.

A nearly cubical box called the explosion cell was constructed next to the wall opposite to the vent. This box was covered with thin polyethylene film to make a gas-tight enclosure. The volume of the explosion cell was varied by moving its back wall forward. Data was obtained with 1.8, 1.35 and 1.0 m^3 explosion cell volumes. The cell was filled with 5 % propane-air mixture and a small fan was run for two minutes to provide mixing of the fuel. After 30 seconds without the fan, the mixture was ignited by a spark.

The specific questions that were attacked were:

1. The effect of varying the volume of the explosion cell with a fixed building volume.
2. The effect of varying the vent area.
3. The response of alternate vent designs.

The test results are given in Table 28.

Table 28. Results of the partial volume explosion tests by Naidus

cell, m^3	A_v = 3.0 m^2, P_{red}, kPa	A_v = 3.0 m^2, dP/dt, kPa/s	A_v = 1.5 m^2, P_{red}, kPa	A_v = 1.5 m^2, dP/dt, kPa/s
1.8	0.91	25.3	1.48	44.3
1.35	1.55	58.4	2.46	66.1
1.0	0.63	29.5	0.56	44.3

For 5 % propane-air mixture S_0 = 0.38 m/s, E = 7.97, P_m = 8.66 bar (Bradley & Mitcheson 1978b) and M = 29.7 g/mol. The value of deflagration index K_G is estimated based on the ratio of burning velocities of propane at 5 % and 4.3 % (Bradley & Mitcheson 1978b), and the value of K_G at the latter concentration (Bartknecht 1993): K_G = 0.38/0.46×100 bar·m/s = 83 bar·m/s. Assuming T = 25 °C = 298 K, c = 340 m/s and a_{cd} = 223 m/s. The methods of Tamanini and Orlando give the predicted values of A_v presented in Table 29. The ratios of predicted to actual values are given in brackets.

Table 29. Predicted vent area of the tests by Naidus

cell, m^3	m	A_v = 3.0 m^2, Tamanini	A_v = 3.0 m^2, Orlando	A_v = 1.5 m^2, Tamanini	A_v = 1.5 m^2, Orlando
1.8	12.0	1.75 (0.60)	2.20 (0.75)	1.40 (0.90)	1.70 (1.15)
1.35	16.0	1.30 (0.45)	1.25 (0.40)	1.05 (0.70)	1.00 (0.65)
1.0	21.6	1.95 (0.65)	1.45 (0.50)	2.05 (1.35)	1.55 (1.05)

In these tests, the flammable mixture was not centrally located in the enclosure as assumed in the derivation of the method by Tamanini and the mixture was not stratified as assumed in the derivation of the Orlando method. The two methods give comparable, although in most cases non-conservative, predictions for the tests by Naidus.

Lamnevik (1996) performed partial volume explosion test with acetylene-air mixture in a garage with floor area of 47 m^2 and a volume of 165 m^3. The room had two open doors with a total area of 4 m^2. The wall area was 210 m^2. An explosion cell was formed by sealing the back end of the room with a polyethylene film (Figure 17). The volume of the cell was selected so that when filled with a stoichiometric mixture it contained 0.5 kg of acetylene.

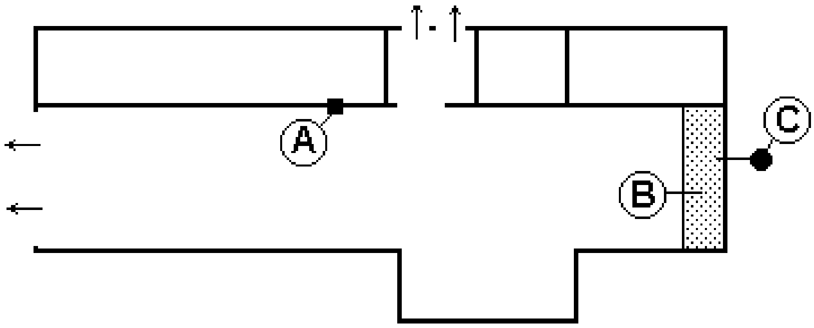

Figure 17. Partial volume vented explosion test of acetylene-air mixture. A – pressure sensor, B – explosion cell, C – acetylene cylinder (Lamnevik 1996).

The mixture was ignited by a pyrotechnic device located on the floor. The measured overpressure was 8 kPa with a rise time of 60 ms. The overpressure peak was followed by a 5 kPa underpressure.

For partially filled enclosures, the redefined dimensionless burning velocity Eq. (101) is inserted into Eq. (102).

For 7.7 % acetylene-air mixture S_0 = 1.44 m/s, E = 8.41, P_m = 8.90 bar (Bradley & Mitcheson 1978b) and M = 28.7 g/mol. The value of deflagration index K_G is estimated based on the ratio of burning velocities of 7.7 % acetylene and 40 % hydrogen (Bradley & Mitcheson 1978b) and the value of K_G of 40 % hydrogen (Bartknecht 1993): $K_G = 1.44/3.45 \times 550$ bar·m/s = 230 bar·m/s. Assuming T = 20 °C = 293 K, c = 344 m/s and a_{cd} = 225 m/s. The density of acetylene is 1.08 kg/m^3 and 0.5 kg in stoichiometric mixture has the volume of 6.0 m^3. The methods of Tamanini and Orlando give the predicted values of A_v presented in Table 30.

Table 30. Predicted vent area of the tests by Lamnevik, A_v = 4.0 m^2

cell, m^3	m	Tamanini	Tamanini/A_v	Orlando	Orlando/A_v
6.0	27.5	1.55	0.40	1.2	0.30

In these tests, the flammable mixture was not centrally located in the enclosure as assumed in the derivation of the method by Tamanini and the mixture was not stratified as assumed in the derivation of the Orlando method. The two methods give comparable, although non-conservative, predictions for the tests by Lamnevik.

7 Summary

Vent dimensioning methods for low-strength enclosures (with reduced overpressures P_{red} no larger than 0.1 bar) have been based on experiments performed in the 1950s and 1960s in relatively small chambers and their validity for large rooms is not known. The reactivity of the mixture is described as the laminar burning velocity S_0 which can be determined with several test methods. The early (1972) method by Runes is still widely used, particularly in the US, although it predicts significantly larger vent sizes than are necessary in non-turbulent explosions, even for large volumes V and/or elongated enclosures.

For high-strength enclosures (with P_{red} larger than 0.1 bar), the correlation by Bartknecht is generally used. The correlation is based on an extensive program of explosion tests of flammable gas mixtures in tanks and silos. The reactivity of the mixture is described by the deflagration index K_G defined as the maximum pressure rise rate times the cube root of the enclosure volume V. The value of K_G has been determined experimentally with small explosion vessels. According to the cube root law, K_G is independent of V. This law is only approximately valid for gas explosions and K_G increases with V. The correlation can be extended to low-strength enclosures by simply omitting the term containing the vent opening pressure.

The increasing magnitude in overpressure with scale arises from the different scale dependencies of the flow and combustion processes in the explosion whose combined influence upon the overpressure can only be inferred by a more fundamentally based model. Empirical explosion overpressure guidelines will therefore have limited accuracy in their ability to extrapolate to full-scale volumes.

Generalised mathematical models for vented explosions have been derived by several authors. The model by Yao (1974) involves a system of three first degree differential equations for dimensionless variables that can be solved numerically. Yao found that experimental pressure-time records with two separate peaks could be predicted only by assuming a time dependent multiplier χ of the burning velocity, called turbulence factor.

Crescitelli et al. (1980) criticised the model by Yao being non-practical because the turbulence factor χ is not related to the measurable parameters. They modified the model to cover the venting of either of unburned mixture or burned gas. Since they had no experimental data where measurement of the discharge coefficient C_d was made, they decided to determine the value of the modified turbulence factor χ/C_d from the tests.

Tufano et al. (1981) combined the two systems of differential equations for unburned and burned gas venting, respectively, in a single one. They concluded that the assumption of burned gas venting was good enough to predict the observed pressure-time records. They also derived a correlation for the prediction of χ/C_d.

Molkov criticised the correlation by Tufano et al. for being independent of the enclosure volume. In 1993, he developed the model by Tufano et al. further assuming that the discharge coefficient μ is not a constant, which can be determined independently of the explosion tests, but a variable. In 2000, he validated the meth-

od with data of vented hydrocarbon-air and hydrogen-air explosions. When the values of χ and μ were selected on the basis of measured P_{red} and the time of its attainment, the predicted overpressure vs. time curves comply reasonably well with the measured ones.

Bradley and Mitcheson (1978) developed a simplified model of vented gas explosion for the purpose of processing the available test data into simple formulas for vent area dimensioning. They used a large amount of test data to derive correlations relating the parameter $\bar{A}/\bar{S}_0$ with the experimental values of P_{stat} and P_{red}. Here $\bar{A}$ is a dimensionless vent area ratio and $\bar{S}_0$ is a dimensionless burning velocity. The value of the discharge coefficient C_d was fixed at 0.6.

Molkov et al. (2000) defined a new dimensionless parameter similar to $\bar{A}/\bar{S}_0$. called the Bradley number Br. Then the turbulent Bradley number Br_t containing the ratio of the turbulence factor χ to the generalised discharge coefficient μ was defined. They proposed a universal correlation for the dimensionless reduced pressure P_{red}/p_0 as a function of Br_t. Finally, an empirical correlation for the dependence of χ/μ on enclosure volume V, Br and dimensionless vent opening pressure P_{stat}/p_0 was derived.

Accidental indoor spills of volatile solvents and the release of flammable gases within buildings and equipment are likely to form flammable mixtures with air if the mixing is inefficient or the ventilation rate is inappropriate. Concentration gradients are formed unless efficient mixing is provided. As a consequence, even small quantities of fuel can form stratified (to the floor) on layered (to the ceiling) flammable vapour or gas mixtures.

If these mixtures are ignited, partial volume deflagrations may occur, possibly producing peak overpressures capable of provoking significant structural damage. Even pockets of flammable mixture occupying a small portion of the total volume are able to produce significant damage in low-strength enclosures.

Flame propagation in stratified mixtures is a very complex phenomenon. In elongated open tunnels, flame propagation through layers corresponding to a homogenous stoichiometric layer of thickness h much less than tunnel height H, experimentally shows a "steady" flame speed about four times S_0 of the stoichiometric mixture. The increased flame speed (with respect to the laminar flame speed) appears to be predominantly due to a fluid dynamic interaction resulting from the combustion of the premixed gas layer. This is valid also for stratified mixtures.

The first model of vented explosions of partially filled enclosures was developed by Tamanini (1996). The model was based on an earlier full volume model for vented gas or dust explosions with a highly idealised set of conditions. The full volume model was used as a tool of data correlation rather than a means to provide an accurate representation of the complex phenomena that characterise vented explosions. The partial volume explosion model had limitations some of which were directly inherited from the full volume explosion model and others arose from the additional assumptions made to adapt the full volume model to the case of partial volume explosions.

Orlando et al. (2007) presented a method for vent dimensioning in enclosures partially filled with stratified or layered gas-air mixtures. They derived an expression for the maximum value of the pressure rise. Based on the tests of flame propagation in stratified or layered gas-air mixtures, they assumed a flame front height of 3h. Then they presented an expression for K_G(m) called "stratified cubic root law" depending on the filling degree m defined as H/h.

Due to scarcity of experimental data on K_G as a function of m, they developed a detailed two-dimensional combustion model to simulate the laminar flame propagation in layered homogenous, stoichiometric methane-air mixtures with different values of the filling degree m, within an elongated closed vessel, with both central and lateral ignition. This model was implemented in a commercial CFD program.

Based on the simulations, they were able to replace the expression for K_G(m) by simply K_G/m. Also Bartknecht correlation for vent dimensioning of high-strength enclosures can be applied simply by replacing K_G by K_G/m. For low-strength enclosures, Bradley and Mitcheson correlations are used. The only modification needed is to divide replace S_0 by S_0/m in the definition of $\bar{S}_0$.

This method has been validated with test data from vented explosions of partially filled enclosures and found to give moderately conservative predictions for almost all the tests. However, the method of Tamanini gave mostly non-conservative predictions. In this report, the idea of Orlando et al. was implemented into the method by Molkov et al. The latter method was found to be inferior to the method by Orlando et al. in that it gave often non-conservative predictions.

The method of Orlando was also applied to tests where a part of the vented enclosure was filled with homogenous mixture. In these tests, the flammable mixture was not centrally located in the enclosure as assumed in the derivation of the method by Tamanini and not stratified as assumed in the derivation of the Orlando method. The two methods gave comparable, although in most cases non-conservative, predictions for the tests.

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