Proceedings Paper:

Fakandu, BM, Andrews, GE and Phylaktou, HN (2016) Gas explosion venting: comparison of experiments with design standards and laminar flame venting theory. In: Gao, W, (ed.) Proceedings of the 11th Symposium of Hazards Prevention and Mititigation of Industrial Explosions, 2016. 11th International Symposium on Hazards Prevention and Mitigation of Industrial Explosions 2016, 25-29 Jul 2016, Dalian, China. ISHPMIE. Article no: paper ISH169, pp. 1320-1332.

Bala M. Fakandu, Gordon E. Andrews* and Herodotos N. Phylaktou (2016). Gas Explosion Venting: Comparison of Experiments with Design Standards and Laminar Flame Venting Theory. Proceedings of the 11th International Symposium on Hazards, Prevention and Mitigation of Industrial Explosions, 11th ISHPMIE, 24-29, July, 2016, Dalian, China. Paper ISH169, Proceedings p.1320-1332 Ed Wei Gao.

Gas Explosion Venting: Comparison of Experiments with Design Standards and Laminar Flame Venting Theory

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Abstract

European and USA design standards for gas explosion venting are quite different in their predictions, with the European standards always giving a higher predicted explosion P_{red} for the same vent coefficient, K_v. The format of the two predictions are different with the US standards following the approach of Swift expressing the vent area as a ratio to the surface area of the vessel, A_s/A_v and the European standard using the vent coefficient approach. $K_v = V^{2/3}/A_v$. It is shown that these two approaches are directly related as A_s is proportional to $V^{2/3}$. The reactivity parameter in the US standards is the laminar burning velocity, U_L, and in the European venting standards it is the deflagration parameter, $K_G = dp/dt_{max}/V^{1/3}$. It is shown that these two reactivity parameters are linearly related. The USA standard is shown to be compatible with spherical flame venting theory and with experimental data other than that of Bartknecht. There is also good agreement with the present results for a 10L vented vessel for which the spherical laminar flame venting theory gives reasonable agreement but predicts the P_{red} to be higher than measured. This is because of the assumption that at the maximum value of P_{red} the bulk flame area is equal to A_s which is not valid. The US standard also has corrections for flame self acceleration, which is a vessel size effect, and for the influence of vessel size on the external explosion, which are not factors addressed in the European standards. The European standard is the equation for the results of Bartknecht for a 10 m^3 vessel and the results of higher and lower volumes in Bartknecht's results are all lower that for 10 m^3. The experimental results reviewed, for methane and propane maximum reactivity vented explosions, include data for vessels larger than that on which the European standards are based and they all give significantly lower values of P_{red} than those of Bartknecht..

Keywords: *venting, mitigation, industrial explosions, overpressure.*

1. Introduction

The reduced overpressure, P_{red}, of any vented explosion depends on the reactivity of the mixture, the volume and shape of the vessel, the ignition position, the position of the vent, the number of the vents, the shape of the vents, the vent static burst pressure, the initial turbulence levels and the presence of obstacles between the ignition point and the vent (Catlin, 1991; Hermanns et al., 2010; Nagy and Verikis, 1983, Hjertager, 1984; Phylaktou and Andrews, 1993; Razus and Krause, 2001; Fakandu et al., 2013, 2014a, b, c, d). This complexity of influences on the venting of gaseous explosions led Hattwig and Steen (2004) to conclude (p.529) that current knowledge does not permit satisfactory predictions of P_{red} for vented gas explosions. European (EN14994:2007) and USA (NFPA 68, 2013) design standards for gas explosion venting are quite different in their predictions, with the European standards always giving a higher predicted explosion P_{red} for the same vent coefficient, K_v. The format of the two predictions are different with the US standards following the approach of Swift (1983, 1984, 1988) expressing the vent area as a ratio to the surface area of the vessel, A_s/A_v and the European standard using the vent coefficient approach, $K_v= V^{2/3}/A_v$. A key problem with both vent design standards is that several parameters that are known to influence P_{red} are not accounted for in the standards or are stated to be negligible effects. These include the position of the ignitor, the shape of the vent, the number of the vents and the position of the vent on the vessel walls. The authors have shown that all of these factors have a significant influence on P_{red} (Fakandu et al., 2013, 2014 a, b, c) but at a level lower than the difference in the US and European vent design standards.

The objective of this work is to review published vented explosion data for methane and propane for a range of vessel volumes with free venting for a range of K_V or A_s/A_v and to compare with incompressible flow venting theory with spherical flame propagation assumptions. It will be shown that arbitrary turbulence factors are not necessary to get reasonable agreement with theory and experiments. The authors (Fakandu et al. 2014d) have previously reviewed data and provided new experimental results for the influence of the static burst pressure, P_{stat}, and showed that both design standards have problems predicting experimental results. The authors (Fakandu et al., 2012) have also previously reviewed the disagreement of design standards with data for 40% hydrogen-air explosions, where sonic flow at the vent occurs and so is a special case and will not be included in this review. However, it was shown that the present approach with compressible and sonic flow orifice equations could predict the experimental results.

2. Physical Phenomena that Can Cause the Peak Overpressure, P_{red}.

There are a range of events in vented explosions that can cause a peak in the pressure time record of a vented explosion. Each event will be illustrated in the results section with an explosion pressure record where that pressure is the maximum overpressure. There are six possible causes of the peak overpressure pressure and in many vented explosions all six pressure peaks may be present and which one is the peak overpressure, P_{max} or P_{red} (Bartknecht, 1993) depends on K_v, K_G, P_{stat},the ignition position and other vent design features. The six pressure peaks were numbered from 1-6 in the order that they normally occur in vented explosions in previous work by the authors (Fakandu et al. 2011, 2012 and Kasmani et al. 2010b) but have been given a more descriptive nomenclature in the present work as summarised in Table 1 and compared with the terminology used by other investigators, where different numbered peaks were used.

P_{burst} is used for the pressure peak associated with the vent static pressure (P_{stat}), which was zero in the present work (Fakandu et al., 2014d). The overpressure due to the pressure loss caused by the flow of unburned gas through the vent (fv) is referred to as P_{fv} in the present work and this is the overpressure predicted by laminar flame theory. Following the P_{fv} pressure peak there is usually a pressure peak, P_{ext}, due to an external explosion and this may be larger or smaller than P_{fv}, depending on the mixture reactivity and K_v. The pressure peak P_{ext} is caused by the turbulent flame propagation of the vented flame in the cloud of turbulent unburned mixture expelled from the vent. It will be shown in the results section that in most vented explosions in the present work either P_{fv} or P_{ext} is the peak overpressure, depending on K_v, K_G and the ignition position.

In some explosions there is an overpressure peak that occurs at the point of maximum flame area (mfa) inside the vented vessel and this will be referred to as P_{mfa} in the present work. The time of maximum flame area is measured in the present work by locating a thermocouple, T_3, flame arrival detector close to the wall in the middle of the cylindrical vessel wall. P_{mfa} is significant as laminar flame venting theory assumes that P_{fv} and P_{mfa} occur at the same time, as discussed below, but the present experiments show that they often occur at different times and that in most cases P_{fv} occurs before the time of maximum flame area and has a higher overpressure than P_{mfa}.

Table 1 Comparison of terminology for the various pressure peaks in vented gas explosions

Peak pressure event	This work End Ignition	Fakandu et al. (2011,2012) Kasmani et al. (2010b)	Cooper et al (1986) Central ignition	Harrison and Eyre (1987) End ignition	Cates and Samuels (1991)	Bauwens et al. (2010) Central ignition
Peak due to vent opening pressure, P_{stat}	P_{burst}	P_1	P_1			
Peak due unburned gas flow through the vent	P_{fv}	P_2		P_{emerg}	ΔP	
Peak due the external explosion	P_{ext}	P_3	P_2	P_{ext}	Dominant	P_1
Peak due to maximum flame area inside the vessel	P_{mfa}	P_4	P_3	P_{max}	Max. burn rate	P_3
Peak due to the reverse flow into the vented vessel after the external explosion. Sometimes co-incident with P_4	P_{rev}	P_5				
Peak due to high frequency pressure oscillations and acoustic resonance.	P_{ac}	P_6	P_4			P_2

In some vented explosions there is a pressure peak, $P_{rev,}$ that occurs after the external explosion, which is caused by the cooling of the gas mixture in the vessel which causes a reduction in the vessel pressure and a subsequent reverse flow of the external gases into the vented vessel, creating turbulence and causing a second explosion in the vessel in the unburned mixture that remained in the vessel. In some vented explosions P_{mfa} and P_{rev} occur at the same time. This occurs because the reverse flow turbulence coupled with a reactive gas mixture can lead to all the mixture inside the vessel suddenly burning. This normally does not occur for vented methane and propane explosions, but has been observed in ethylene and hydrogen explosions.

This reverse flow explosion is followed by an oscillating mass flow out of the vent and then back into the vessel, which gives a low frequency pressure oscillation. This is quite different from the high frequency acoustic pressure oscillations referred to by Cooper et al (1986), which are referred to as P_{ac} in the present work. P_{ac} is caused by oscillatory combustion inside the vessel and unburned gas trapped in corner regions of the vessel and burning after the flame has left the vent. Which of these six pressure peaks is the maximum will be shown in this work for free venting to depend on K_v, K_G and the ignition location.

Most investigations of vented explosions do not give the pressure time diagrams and simply report P_{red} with no comment on whether this is P_{fv}, P_{ext}, P_{mfa}, P_{rev} or P_{ac}, using the present terminology. Cooper et al (1986) do not refer to a pressure peak associated with the maximum flow of unburned gas through the vent, nor do Bauwens et al. (2010). This is surprising as the classic laminar flame venting theories are all based on predicting P_{fv} for free venting (Bradley and Mitcheson, 1978; Cates and Samuels, 1991; Molkov, 2000). None of the previous investigators refer to the present P_{rev} reverse flow pressure peak. Fakandu et al. (2014a) show that this was a significant pressure peak in central ignition vented explosions, but is rarely the dominant overpressure.

The present experimental 10L cylindrical vented vessel with an L/D of 2.8 and with end ignition has been described in several previous papers (Fakandu et al. 2003, 2004 a, b, c, d) and will not be duplicated here. The vented vessel was instrumented with flame position detectors and internal (PT0) external pressure transducer (PT2) to ensure the unequivocal identification of the various pressure peaks. A flame arrival detector T_4 was located in the plane of the vent to determine the time the flame exited the vent and hence to distinguish when P_{red} was due to the external explosion, which must occur after this time. The external pressure transducer also confirmed this as there was no pressure rise until the flame left the vent. The flame arrival thermocouple detector T_3 was used to determine the time of maximum flame area, mfa. There were other thermocouples fitted to determine the flame speeds upstream and downstream of the vent. Fig. 1a shows a typical pressure record for 10% methane/air with end ignition and all the pressure peaks associated with those in Table 1 are marked and it is shown that in this case with K_v = 5.4 P_{ext} was the dominant overpressure. In contrast Fig. 1b shows the pressure record for K_v = 10.9 for 10% methane-air vented explosions, where the P_{fv} is the dominant overpressure, but where the flame arrival at the vent at T_4 and the peak pressure coincide. This means that the external overpressure must occur after the peak pressure and be one of the peaks on the pressure decay. For both pressure records the time of maximum flame area upstream of the vent, T_3, has been marked and this is not the event that causes the peak pressure.

The co-incidence of P_{red} with the time of flame arrival at the vent, T_4, can lead to the erroneous assumption that this is P_{ext}, as many investigators interpret the timings from video records. There has to be a time delay between the flame arriving at the vent and the downstream turbulent unburnt gases being ignited by the flame after it has emerged from the vent, by which time the peak pressure has decayed and is not controlled by the external explosion. The external explosion is controlled by the turbulence in the flow of unburned gases out of the vent, which is controlled by the pressure loss at the vent and this is controlled by the dynamic head of the velocity at the vena contracta downstream of the vent. The external explosion overpressure occurs due to fast turbulent flames and an overpressure generated by Taylor's equation (1946) as shown below, where M is the flame speed Mach

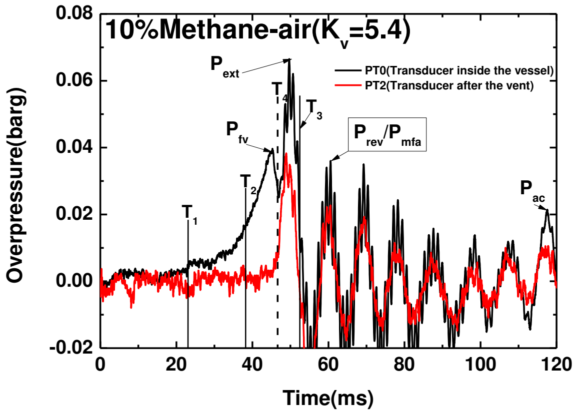

Fig.1a:Pressure time record for free venting for K_v = 5.4 with end ignition

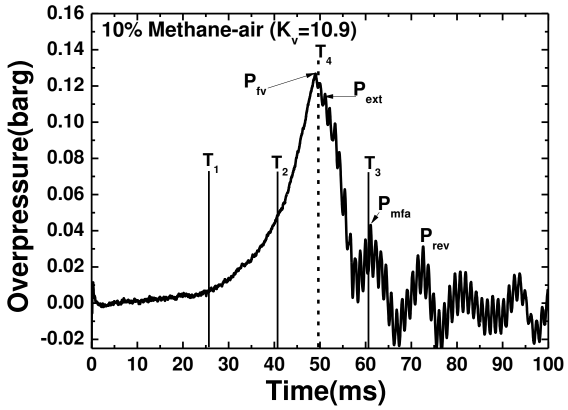

Fig. 1b: Pressure time record for free venting for K_v = 10.9 with end ignition

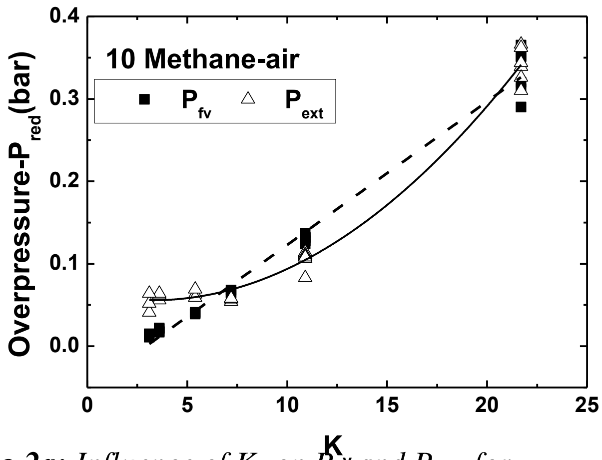

Fig.2a: Influence of K_v on P_{fv} and P_{ext} for 10% methane-air with end ignition.

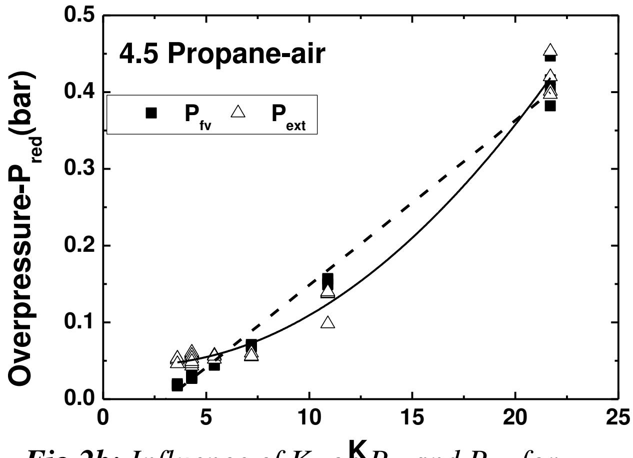

Fig.2b: Influence of K_v on P_{fv} and P_{ext} for 4.5% propane-air with end ignition.

number. This does give a mechanism for the volume of the vented vessel to influence the overpressure as the turbulent flame speed is a function of the length scale of turbulent, L. This is proportionate to the vent diameter and this increases with vessel volume for the same K_v. Fakandu et al. (2013) showed the importance of this by increasing the number of vents from 1 to 4 for the same K_v which reduces the vent diameter and the external flame turbulent burning velocity. This was shown for 10% methane-air vented explosions to reduce the overpressure for situations where P_{ext} controlled P_{red} (K_v < 5.4). However, the reduction in P_{ext} with the smaller length scale was only 12% for half the vent diameter. This was because the dependence of turbulent burning velocity on L is relatively low at about $L^{0.25}$ (Phylaktou et al., 1994).

$$\frac{P_+}{P_o} = \frac{2\gamma M^2}{1+M} \qquad (1)$$

Fig. 2 shows P_{fv} and P_{ext} as a function of K_v for 10% methane-air and 4.5% propane-air for the 10L vessel with end ignition. This shows that there is a critical K_v of >5.4 for methane and propane for P_{fv} to be >P_{ext}. Fig. 2 also shows the non-linear dependence of P_{ext} on K_v as expected from Eq. 1. In contrast there is a linear dependence of P_{fv} with K_v and the two relationships cross at the critical K_v. For the reasons give above this critical K_v is likely to be higher for large vented vessels, due to the influence of L on M and hence on $P_{ext.}$

3. Laminar Flame Venting Theory for Free Venting

The overpressure due to the flow of unburned gases through the vent, P_{fv}, can be predicted from the orifice plate flow equation once the maximum unburned mass flow through the vent has been modelled. This also has to be modelled to order to predict P_{ext}, and hence the prediction of both of these overpressures are interlinked. Most theories of explosion venting assume that flow through the vent dominates the overpressure and that P_{fv} is the dominant overpressure (Bradley and Mitcheson, 1978 a, b; Swift, 1983; Cates and Samuel, 1991; Molkov, 1999, 2000). Bradley and Mitcheson (1978a) reviewed the development of laminar flame venting theories and showed that unburned gas venting had much higher overpressures than burnt gas venting. They also showed the common features of laminar flame venting theories were: unburned gas venting; the use of orifice plate flow equations; and the maximum induced unburned gas flow rate as the mass flow through the vent. Andrews and Phylaktou [2010] and Kasmani et al. [2010b] have reviewed laminar flame venting theory based on the pressure loss of the flow of unburned gas through the vent as the dominant overpressure for free venting. The classic laminar flame venting model [Bradley and Mitcheson, 1978a] assumes that a spherical flame in a spherical vessel with central ignition propagates uniformly until all the unburned mixture ahead of the flame is expelled through the vent. The maximum overpressure is then the vent orifice flow pressure loss at the maximum unburned gas vent mass flow rate (Andrews and Phylaktou, 2010). The unburned gas mass flow rate is the flame surface area, A_f, times the unburned gas velocity ahead of the flame, $U_L(E_p-1)$, times the unburned gas density, ρ_u. A further assumption is made that simplifies the theory and this is that the maximum possible flame area is the surface area of the vessel walls, A_s. This essentially assumes that the maximum flame area controls P_{red} which has been shown above to not be supported by the present evidence or the transparent vessel high speed photography of Nagy and Verikis (1983). This assumption was first proposed by Runes (1972) and should ensure that the theory overpredicts experimental results.

The laminar flame venting model with the above assumptions, leaves the prediction of P_{red} a function of A_v/A_s, as shown in Eq. 2 (Andrews and Phylaktou, 2010). Bradley and Mitcheson (1978 a, b), Swift (1983, 1984, 1988) and Molkov (1999, 2000) all left the theoretical venting equation in terms of A_v/A_s. The Swift formulation of the laminar flame venting theory has been adopted in NFPA 68 2013. In the original Swift (1983) formulation of Eq. 2 a turbulence factor of 5 was assumed, but this has been replaced with λ and a procedure given in NFPA 68 2013 to evaluate this. Alternative procedures to calculate λ have been given by Molkov (1999, 2000).

$$A_v/A_s = C_1\ \varepsilon^{-1}\ \lambda\ U_L\ (E_p-1)\ \ P_{red}^{-0.5} \quad \text{with } P_{red} \text{ in Pascals} \qquad (2)$$

where $C_1 = \rho_u^{0.5}/(C_d\ 2^{0.5}) = 1.27$ for $\rho_u = 1.2$ kg/m^3 and the vent discharge coefficient C_d = 0.61.

With P_{red} in Eq. 2 converted to bar and the above value for C_1 inserted and an E_p of 8.05 used, which is the adiabatic value for propane, Eq. 2 becomes Eq. 3 for P_{red} in bar.

$A_v / A_s = 0.0283\ \varepsilon^{-1}\ \lambda\ U_L\ P_{red}^{-0.5}$ (3)

The constant in Eq. 2 becomes 0.0247 if a C_d of 0.7 is used, as in the work of Swift (1983) which is the C_d value adopted in NFPA 68 2013 as Eq. 4. This predicted value of the constant in Eq. 4, which is Eq. 7.2.1.1 in NFPA 68 (2013) is only 11% higher than Eq. 2 and so the laminar flame venting theories have very similar results. Clearly NFPA 68 (2013) is based on laminar flame venting theory with the assumptions detailed above.

$A_v/A_s = C\ P_{red}^{-0.5} = 0.0223\ \lambda\ U_L\ P_{red}^{-0.5}$ for P_{red}<0.5 bar (4)

The value for C_d of 0.61 in the constant C_1, is a common assumption in venting theory, but it only applies where the vent is small relative to the upstream vessel cross sectional area, A_1 which is a high K_v assumption. It can be derived from ideal fluid flow theory as the ratio of the vena contracta area to the sharp edged orifice area. Table 2 shows the predictions of ideal fluid flow theory for C_d as a function of the orifice Area, A_2, to upstream flow area, A_1, which is effectively $1/K_v$ for cylindrical vessels. This shows that for low K_v a more appropriate value for C_d is 0.7 and this is the value used in NFPA 68 (2013), but using the values in Table 2 as a function of K_v would be more accurate.

Table 2 Variation of C_d with K_v for Sharp Edged Vents

A_2/A_1	0.05	0.1	0.2	0.4	0.6
K_v	20	10	5	2.5	1.67
C_d	0.61	0.62	0.632	0.65	0.712

A further problem with the constant C_1 in Eqs. 2 and 4 is that the density of the unburnt gas upstream of the vent, ρ_u, increases with P_{red}. Once the vent is open compression of the gases is no longer adiabatic, but most theories of venting assume adiabatic compression to compute ρ_u, as was done by Swift (1983, 1984, 1988) and adopted by NFPA 68 (2013). Lunn (1984) fitted the empirical relationship in Eq. 5 to relate the increase in unburnt gas temperature due to the P_{red} and from this the density may be calculate, as illustrated in Table 3.

$$T = 298\ [(P_{red} + P_a)/P_a]^{0.286} \qquad (5)$$

Table 3 Variation of unburnt gas density with P_{red} (Lunn, 1988)

P_{red}	T K	ρ_u kg/m^3	Relative density to 1.2 kg/m^3
0.1 bar	306	1.25	1.04
0.2 bar	314	1.33	1.11
0.5 bar	335	1.56	1.30
1 bar	363	1.92	1.60
2 bar	408	2.56	2.13

For a P_{red} of 0.5 bar Eq. 5 predicts a 30% increase in ρ_u which increases C_1 in Eq. 2 to 1.45, which is an increase by a factor of 1.14. This is not a large effect and vents are normally designed for P_{red} <0.5 bar.

ε in Eq. 2 is a compressible flow factor for orifice plate flow, this is the ratio of compressible to incompressible flow and cannot be calculated based on nozzle flow, as is done is all previous venting theories, as an orifice plate type vent is not a nozzle and the compressible flow occurs in free space at the vena contracta and is not bounded by walls. Empirical data from orifice plate flow metering, based on comparing water flow with air flow, shows that Eq. 6 gives a correlation for ε (BS ISO TR15377:1998).

$$\varepsilon = 1 - \{[0.41 + 0.35(1/K_v)^2]\, P_{red}/[\gamma\, (P_i + P_{red})\} \quad (6)$$

For K_v >5 the K_v term in Eq. 5 is negligible. For a K_v of 2 and P_{red} of 0.1 bar and the ratio of specific heats γ=1.4 Eq. 5 gives ε = 0.97 and is hence a small correction. For a P_{red} of 0.5 bar, K_v =5 and the ratio of specific heats γ=1.4 Eq. 5 gives ε = 0.90 and this is normally a worst case vent design. In the following discussion ε will be taken as 1, but can easily be corrected for using Eq. 6.

As P_{red} increases compressible flow becomes more important and eventually critical flow occurs at the vent with sonic flow at the vena contracta. For air the critical flow pressure ratio is 1.893 ~ 1.9, which is a P_{red} of 0.9 bar in a vented explosion. It may be shown for critical flow in a nozzle (no equivalent equation has been produced for an orifice) that the mass flow rate, m_v, through the area A_v is given by:

$$m_v = [(\gamma+1)/2]^{1/(1-\gamma)}[2\gamma/(\gamma+1)^{0.5}1/(RT_o)^{0.5}P_oA_v$$

For air γ=1.4, R=287.04 J/kgK

$$m_v = 0.0404\, P_o/T_o^{0.5}A_v = 0.00233\, P_oA_v \text{ for } T_o\text{=300K} \quad (7)$$

where $P_o = P_a + P_{red}$

Note that T_0 is the temperature of the unburnt gas upstream of the vent, it may be estimated from P_{red} as in Eq. 3. Note that the $P_{red}^{0.5}$ relationship with mass flow rate no longer applies and a constant exponent for the P_{red} term in the vent design equation cannot be used for all P_{red} up to 2 bar, as is done in the European venting standards based on Bartknecht's (1993) data. In this work the laminar flame venting theory will use Eq. 7 as the vent flow equation for P_{red}>0.9. There is then no reason to limit the application of laminar flame venting theory to P_{red}<0.5 bar as is done in NFPA 68 (2013). The experimental data of Bartknecht (1993), which goes up to a P_{red} of 4 bar supports this approach.

It may be shown that the laminar flame theory of Bradley and Mitcheson [1978a] for free venting can be expressed in the above format as in Eq. 8.

$$A_v/A_s = 0.831[\lambda\, U_L(E_p - 1)] / [C_d\, a_v\, P_{red}^{0.5}] = 0.0284\, \lambda\, U_L\, P_{red}^{-0.5} \quad (8)$$

where a_v is the velocity of sound at the vent, taken as 343 m/s for air. E_p has been taken as the adiabatic value for propane of 8.05. Eq. 8 is identical to Eq. 3. There was a difference in C_d of 0.6 instead of 0.61 used in Eq. 8, but this only changes the constant in Eq. 8 to 0.0280. Bradley and Mitcheson (1978b) went on to use a value for the turbulence factor λ of 4.19 to produce a prediction that would encompass data from vented explosions with a static burst pressure at the vent. Eq. 8 also shows that the artificial dimensional numbers used by Bradley and Mitcheson are unnecessary as the 0.0284 U_L term in Eq. 8 has units of bar$^{-0.5}$ so that Eq. 8 is dimensionless. Molkov et al. (1999, 2000, 2013) refers to the ratio of λ/C_d in Eq. 8 as the DOI number which has very similar components as for λ in NFPA 68 (2013).

The A_v/A_s formulation of the laminar flame venting equation can be converted into a form using the vent coefficient K_v as $A_s = C_2 K_v$, where C_2 is 4.84 for a sphere, 6 for a cube and 5.54 for a cylinder with L/D=1 and 5.86 for the present cylinder with an L/D of 2.8. This then converts Eq. 2 into Eq. 9 and this has the same form as in the European vent design guidance [Andrews and Phylaktou, 2010 and Kasmani et al., 2010b].

$$1/K_v = A_v/V^{2/3} = C_1C_2\ \varepsilon^{-1}\ \lambda\ U_L\ (E_p\text{-}1)\ \ P_{red}^{-0.5} \qquad (9)$$

If Eq. 9 is used for a cube and P_{red} is converted from Pa to bar then with $E_p = 8.05$ Eq. 9 becomes Eq. 10.

$$1/K_v = 0.170\ \varepsilon^{-1}\lambda\ U_L\ P_{red}^{-0.5} \qquad (10)$$

For propane with U_L=0.46 m/s and taking $\varepsilon = 1$ and $\lambda = 1$ Eq. 10 becomes Eq. 11.

$$1/K_v = 0.078\ P_{red}^{-0.5} \qquad (11)$$

For 10% methane –air with U_L as 0.42 m/s (Satter et al., 2014) and E_p as 7.54 the constant in Eq. 9 becomes 0.066. For 40% hydrogen –air with U_L taken as 3.5 m/s and Ep as 6.47 the constant in Eq. 9 becomes 0.46.

Eq. 11 is in the same format as used by Bartknecht (1993) for P_{stat} = 0.1bar and his constant for propane was 0.200. Bartknecht's (1993) Equation for propane for P_{stat}=0.1 bar is given by Eq. 12 and the constant in Eq. 12 was 0.164 for methane

$$1/K_v = 0.200\ P_{red}^{-0.58} \qquad (12)$$

This implies a λ value in Eq. 10 of 2.56 for agreement which gives a 6.57 factor difference in P_{red} for the same K_v. If the Bartknecht 10 m^3 vented data is ignored as not agreeing with his data at four other volumes, then a λ of only 2.1 is required for agreement. The Bartknecht (1993) correlation, used in the European venting standards for gases, is based on his data for a 10m^3 vessel and does not fit his data for any other volume that he tested from 1 – 60 m^3.

For methane Bartknecht had a constant in Eq. 11 of 0.164 and the above prediction needs a turbulence factor λ of 2.50 for agreement (or 2.0 if the 10 m^3 data is ignored), which gives a 6.2 factor difference in P_{red} for the same K_v. These turbulence factors for propane and methane are very similar. The used of 100mb P_{stat} in Bartknecht's work cannot account for such high values of λ (Fakandu et al, 2014d) and there must be some other factor that caused the higher overpressure in his work. This factor could be ground effects on the external jet flame, as Bartnecht's test vessel were all mounted on the ground and a Coanda effect on the external jet could have increased the overpressure. It will be shown in the next section that no other experiments have found the high overpressures found in Bartknecht's (1993) work and most are closer to agreement with Eqs. 2 and 4.

4. Comparison of the Laminar Flame Venting Theory, NFPA 68 (2013) and Bartknecht's Venting Correlation with Experimental Data

The above laminar flame theory in Eqs. 2 and 4 is compared in Fig. 3 for 10% methane-air and in Fig.4 for 4.5% propane-air with the present experimental results in a 10L vessel with end ignition and those in larger vented vessels in the literature. Literature values for free venting or for the lowest P_{stat} investigated has been used. The data is plotted both as A_v/A_s and $1/K_v$ with the relationship plotted as a common axis as for cubic explosion vessels, which applied for a lot of the literature data. This shows excellent agreement of the laminar flame theory with the experimental results for near free venting of a 35 m^3 vessel of Papas and Foyn (1983) and of the 64m^3 vented vessel results of Bauwen et al. (2010) and of the 25m^3 vented vessel results of Bromma (1957). A further difference between Eq. 9 and the Bartknecht vent design equation is that the P_{red} exponent is -0.58 compared with -0.5 for incompressible flow in the vent. If Bartknecht's data in the sonic flow regime is ignored and the data fitted to P_{red}<0.5 bar then Bartknecht's results support a -0.5 pressure exponent. Consequently, the difference between the Bartknecht vent design P_{red} exponent and the theory is not considered to be important.

Bartiknecht's data in Figs 3 and 4 is for P_{stat} = 0.1 bar and is not for free venting. This is particularly significant at low P_{red} and low K_v, where the predictions would be <0.1bar for a free vent. Bartknecht's correlation has been corrected to that of a free vent in Figs. 3 and 4 in two ways: firstly simply deduction 0.1 bar from the predicted P_{red} and secondly by applying Bartknecht's P_{stat} correlation and exptrapolating it to free venting, which is a very small correction. It would have been preferable for the design correlation to have the first term for an open vent and then an additional term valid for any value of P_{stat}. In the present work P_{stat} is zero, as open vents were used. The reasonable agreement between the literature data and the laminar flame venting equations, support the assumption that it is the flow of unburned gases displaced by the expanding flame that controls the peak overpressure. This flow controls P_{fv} which in turn controls the magnitude of the vented jet velocity and the jet induced turbulent flow that creates the external explosion outside of the vent, P_{ext}.

For methane explosions the only data that supports Eq. 12 for methane is that of Bartknecht (1993) for vented vessels of 1 and 30 m^3. The three lines for laminar flame venting theory are different mainly in the C_d value used and span the range of possible C_d as K_v is varied. These predictions, that are the basis of the NFPA 68 (2013) design procedures, should lie above experimental data due to the assumption that the flame area is the surface area of the vessel, which is conservative. Fig. 3 shows that all the present results fall on the predicted line at low K_v or below it at high K_v and 21 data points in the literature also lie on or below the predicted lines. This includes the 35 m^3 data of Solberg (1979) and the very largest volume work of

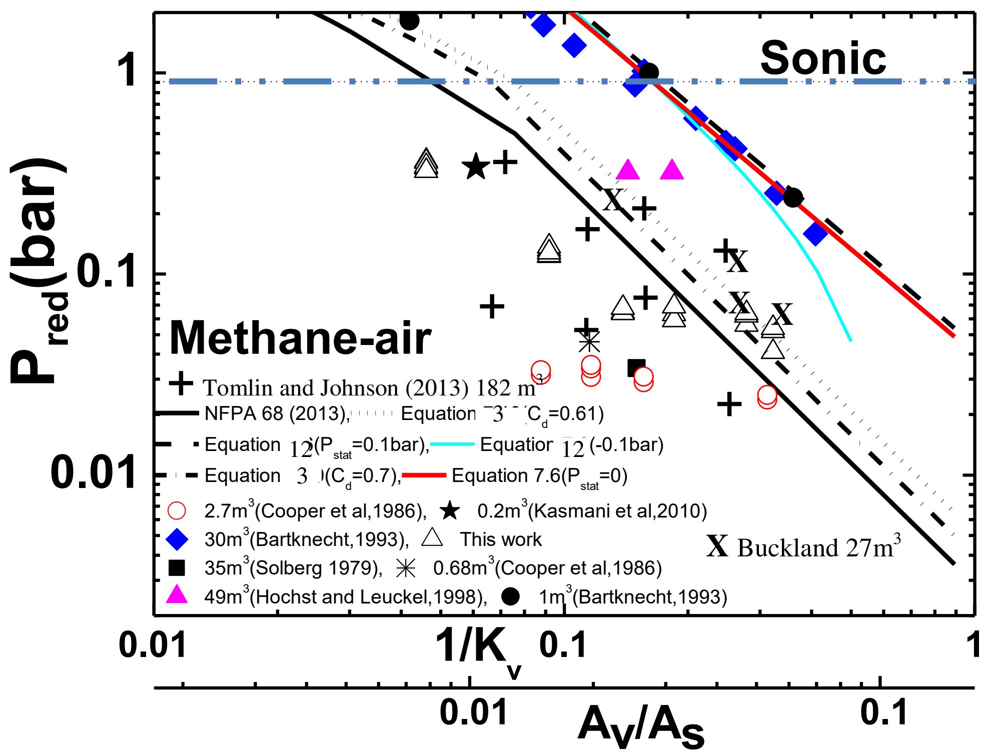

Fig. 3. P_{red} *as a function of 1/*K_v *and* A_v/A_s *(for a cube) for 10% methane-air. Comparison with the laminar flame theory Eq. 3, NFPA 68(2013) Eq. 4 and EN14994:2007. Eq. 12 with Bartiknecht's and other literature experimental data and with the present results of a 20L vented vessel with L/D = 2.8 and end ignition.*

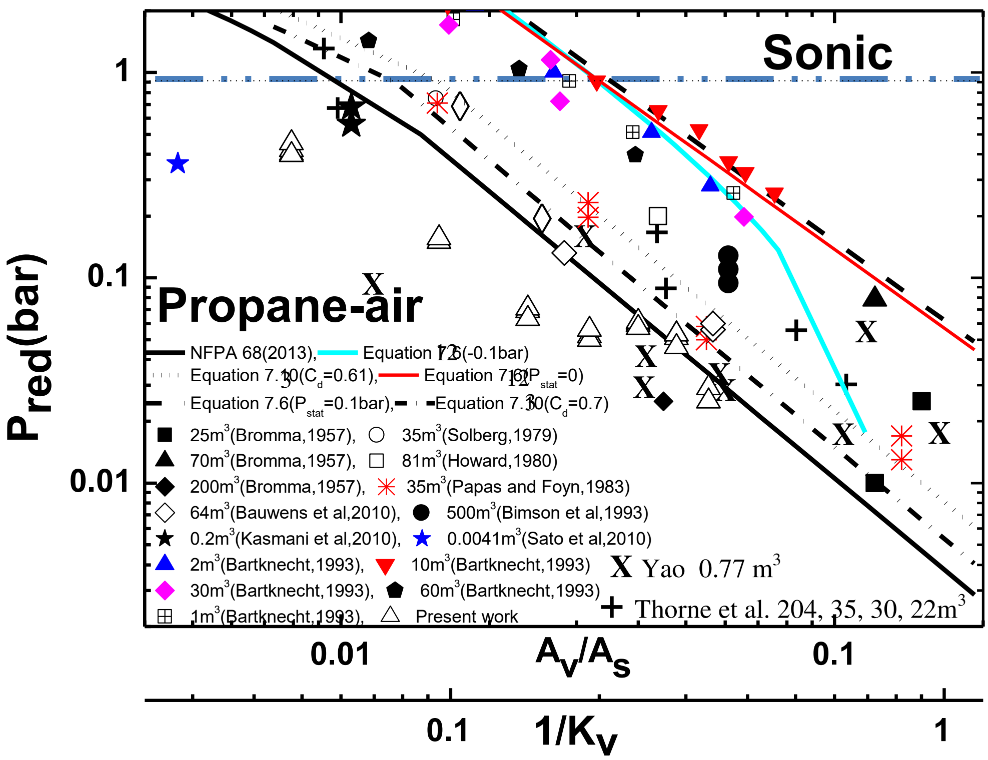

Fig. 4 P_{red} *as a function of 1/*K_v *and* A_v/A_s *(for a cube) for 4.5% propane-air. Comparison with the laminar flame theory Eq. 3, NFPA 68(2013) Eq. 4 and EN14994:2007. Eq. 12 with Bartiknecht's and other literature experimental data and with the present results of a 20L vented vessel with L/D = 2.8 and end ignition.*

For propane explosions in Fig. 4 there is more data in the literature, but none of it supports the Bartknecht design equation. All of Bartknecht's results for volumes 1-60 m^3 are well above those of other investigators, but only the 10 m^3 data agrees with his design Eq. 12. All the present results agree with the laminar flame venting Eqs. 3 and 4 or are below them. There are 25 data points from the literature in Fig. 4 that are close to the laminar flame venting equations used in NFPA 68 (2013) or are below them. The data of Papas and Foyn (1983) for a 35 m^3 vessel lie directly on the laminar flame venting line for Eq. 3 However, there are 7 data points above the predicted lines and these may indicate an influence of vessel volume.

In NFPA 68 (2013) there are a series of factors that contribute to the term λ in Eq. 3. Two of these have a dependence on the vessel volume in addition to that included in K_v or A_s. The first is flame self acceleration and the second is the external explosion. The self acceleration term follows the procedures of Chippet (1994) and is related to the vessel volume as the flame diameter is larger the bigger the vessel volume for the same K_v. It may be shown that the correction of this has a $V^{0.13}$ dependence, which for a factor of 1000 increase in volume is only a 2.5 increase in the constant in Eq. 3 or a factor of 6 increase in P_{red}. This is of the order of the volume effect in Figs. 3 and 4. The second factor is the external explosion turbulent length scale which is a function of the vent diameter. NFPA 68 has a $D_v^{0.0487}$ dependence for constant K_v or A_v/A_s which is a dependence of $V^{0.0162}$ and for a factor of 1000 increase in V this is a 1.12 increase in the constant in Eq. 3 which gives an increase in P_{red} of 25%.

The volume effect is not sufficient to account for the differences in Bartknecht's (1993) results from the rest of the literature, nor is the effect of spark position, vent shape, vent number etc sufficient to account for these differences, although they contribute to the data scatter in Figs. 3 and 4. There is an additional factor generating turbulence in Bartknecht's data, as discussed above. This may be the presence of the ground close to the vent giving a coanda effect and ground interaction turbulence increasing the external explosion overpressure. Further work is required to demonstrate that this effect is significant.

Conclusions

1. Free vented explosions in a small 0.01 m^3 vessel with an L/D of 2.8 were investigated, as it was considered that this size would produce a laminar flame explosion that would enable laminar flame venting theory to be validated without empirical turbulence factors. End ignition was used as this gave a higher overpressure than for central ignition.
2. The results showed that the P_{red} due to the flow through the vent, P_{fv}, was the dominant overpressure for methane and propane explosions for $K_v > 5.4$ and for lower values of K_v the external explosion, P_{ext}, was dominant.
3. The form $1/K_v = a\ P_{red}^{-n}$ of the venting design equations of Bartknecht, for P_{stat}=0.1bar, was shown to be the same as in the Swift approach that is recognised by NFPA 68. For agreement with Bartknecht's results for methane and propane venting the laminar flame venting theory needs a burning velocity turbulence factor of 2.60 and 2.5 respectively. A possible source of this turbulence in Bartknecht's work is the interaction of the vented jet with the ground, as all Bartknechts vessel had the bottom of the vessel on the ground and most other workers elevated the vessels well clear of the ground.
4. For the present 10L vented vessel, the laminar flame venting theory over predicts the measurements for methane and propane, due to the assumption of the maximum flame area being A_s and the actual flame area at the time of maximum overpressure being less than this.
5. The laminar flame venting theory is the same as that of Bradley and Mitcheson [1978] and Swift [1983] if the same vent orifice discharge coefficient C_d is used. The adoption of the Swift [1983, 1984 1988] approach to laminar flame venting design for P_{red} up to 0.5

bar in NFPD 68 2013 is justified as it is in good agreement with the present results and with many other vented explosion results in the literature. There was no requirement to invoke a turbulence factor to force agreement between the predictions and measurement for low P_{stat} vented explosions. For most of the data the basic laminar flame theory with λ=1 agreed with the literature data or was higher than the data. There were only a few literature P_{red} higher than the predictions, that may have been related to a volume effect in addition to that embedded in K_v.

Acknowledgements

Bala Fakandu would like to thank the Nigerian Government for a Petroleum Fund Scholarship. We would like thank Gary Tomlin of DNVGL for informative discussions on gas explosion venting.

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