

TURBULENCE GENERATED DURING VENTED GASEOUS DEFLAGRATIONS AND SCALING ISSUE IN EXPLOSION PROTECTION

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The paper presents recent findings in quantitative evaluation of turbulence generated during venting of gaseous deflagrations in empty enclosures without initial turbulence. A correlation dependence of venting generated turbulence is presented and discussed. The scale of enclosure and the Bradley number are shown to be the main parameters influencing the turbulence generated during venting. This correlation is a part of the innovative vent sizing technology that is based on two correlations, which are valid for various combustible mixtures and enclosures of arbitrary volume and strength. The conservative form of the universal correlation for vented gaseous deflagrations is presented for the first time. The result of a comparison between the suggested conservative vent sizing formulas, experimental data and predictions by the empirical vent sizing technique of NFPA 68 standard (Edition 1998) is given. The influence of turbulence generated during venting on vent sizing of enclosures with inertial vent covers is analysed and equation for scaling of upper limit for vent cover inertia is suggested. The upper limits for vent cover inertia are estimated for enclosures of different volume from 0.1 to 1000 m^3, which can withstand the same maximum overpressure of 30 kPa and have the same vent cover release overpressure of 3 kPa. Results have demonstrated that inertial vent covers have 100% "efficiency", if inertia is below an upper limit calculated by the suggested equation, even the absolute value of inertia is much higher than the level that has been accepted so far.

INTRODUCTION

As a part of the new ATEX Directives, manufacturers of venting devices have to provide vent efficiency data, including vent inertia effects [1-2]. Although the effect of turbulence has been generally acknowledged as being a major factor in the development of gas explosions, no quantitative measurements have been attempted to assess the extent of this effect in venting experiments with inertial vent covers until today.

Butlin concluded in 1975 that turbulence should be studied in future work [3]. In 1978 Anthony underlined again that the production of an adequate mathematical model for vented deflagration would depend on resolving the problem of turbulence [4].

Some models, as those for dust explosions [5], assume that the flame propagates throughout the entire event at a constant effective burning velocity. This is probably a fair enough approximation in dust explosions, where turbulence generally dominates the combustion process. However, it is not true for situations involving gas mixtures, where the venting process itself is known to cause the flame to accelerate [5]. Governing equations for turbulent vented gaseous deflagrations were derived from the first principles in paper [6]. The inverse problem method for vented gaseous deflagrations has been developed [7] and efficiently used over the years of research allowing to gather unique data on venting generated turbulence. For example, an analogue to the Le Chatelier-Brown principle for vented gaseous deflagrations [7] was revealed by this method. On the way to an innovative performance-based vent sizing technology a generalization of international experimental data was performed for vented gaseous deflagrations and the universal correlation was revealed for the first time in 1995 [8] followed by the closure of this fundamentally new vent sizing approach with the correlation for venting generated turbulence, which was presented for the first time two years later in 1997 [9]. Two of our previous articles were devoted to the problem of inertial vent covers in explosion protection [10-11]. Recently our original correlations for vent sizing were developed further to include experimental data on fast burning mixtures, such as near stoichiometric and rich hydrogen-air mixtures, and test data on elevated initial pressures [12-15].

There are some statements on inertia effects in early publications. For room-size enclosures in high turbulence tests with specified fasteners inertial effects appear to be evident with panels weighing 6 kg/m^2 or more and the data obtained suggest that the influence of inertial effects on the release pressure cannot be ignored for panels weighing more than about 10 kg/m^2 [16]. A recent paper [1] states that above the 60-m^3 vessel volume, the effect of panel inertia up to 10 kg/m^2 can be taken to be negligible, but below there is uncertainty. Other sources suggest similar values, for example Cooper [2] states that, as the volumes increase beyond 100 m^3, doors with a mass of less than 20 kg/m^2 could be employed with little or no penalty on the predicted reduced pressure. However, there are still questions: do these conclusions still hold for near-stoichiometric hydrogen-air and very lean/rich hydrocarbon-air mixtures, and what is the penalty, if any, for higher inertia? To neglect the influence of vent cover inertia Bartknecht [17] suggests a mass of less then 10 kg/m^2, whereas NFPA 68 suggests approximately 12 kg/m^2. In the UK, values of up to 25 kg/m^2 have been acceptable in the past, with some vents being more than 40 kg/m^2 [2]. The Russian standard SNiP II-90-81allows the inertia of relief panel of 120 kg/m^2! Can the inertia values given above be applied to any enclosure volume, vent area and combustible mixture?

The problem of scaling for vented gaseous deflagrations with inertial vent covers does not seem to have received a great deal of attention. Most of the works have been done on too small a scale to be applicable to deflagrations in large-scale enclosures, including buildings. Research should therefore include derivation of the scaling laws [4]. That is the main objective of this paper.

VENTING GENERATED TURBULENCE

It is well known today that vent opening will facilitate the distortion of flame front due to reasons of different physical nature. Numerous types of flame front instabilities, its cellular or fractal structure development and large-scale flame front–flow interactions are some of these reasons. As a result, the burning velocity in vented vessel is known to exceed its value for laminar spherical flame in up to 100 times, depending on conditions. The *turbulence factor* is a widely accepted concept that characterises the augmentation of burning velocity or, what is more correct, the flame front area with respect to the ideal case of laminar spherical flame propagation. Until 1995 the data on the turbulence factor obtained by different authors were not correlated [8, 18]. The main barrier was the use of different models, some of which have been built on unacceptably rough assumptions.

It is clear that the turbulence factor is not constant in the course of vented deflagration. The turbulence factor increases with the vent opening when combustion in a closed enclosure proceeds to deflagration in the vented one. It can grow up or slow down in course of vented deflagration depending on conditions. The turbulence factor can decrease due to flame laminarisation close to walls, flame extinction, etc. Nevertheless, in our approach we use the constant turbulence factors – one before venting, χ_0, and another one after the vent cover is released, χ. This is a simplification. However, it coincides with the conclusion by Swift et al. [19], who attempted to employ a variable turbulence factor in their analysis: "it seems best to employ a constant turbulence correction factor and gain the corresponding simplicity, rather than to carry more elaborate equations through a train of numerical computations whose accuracy is also limited to only a narrow range of experimental conditions".

The attempts to produce any reasonable correlations for venting generated turbulence have failed for another reason as well – due to a neglect of the role of the generalised discharge coefficient, μ, which is dependent on vented deflagration conditions. This fact of discharge coefficient dependence on conditions was recognised already about 20 years ago by various authors. It has been demonstrated in a series of studies that reduced explosion pressure correlates with the *deflagration-outflow interaction (DOI) number*, that is the ratio of the turbulence factor, χ, to the discharge coefficient, μ, rather than with the turbulence factor alone. Tufano et al. [20] paid particular attention to this issue. They have recommended the following correlation for the DOI number (*effective turbulence factor* in their terminology)

$$\chi / \mu = 0.51 \cdot W_C^{0.6} \cdot \exp(-0.27 / \pi_v^3) , \tag{1}$$

where the venting parameter is

$$W_C = \frac{\mu F A}{V} \frac{c_{ui}}{S_{ui}} \sqrt{\frac{2}{\gamma - 1}} , \tag{2}$$

that is close to our Bradley number (see equation (4)).

We have processed experimental data on vented gaseous deflagration in a wider range of conditions than in [20] and have obtained the correlation for venting generated turbulence in the form [15, 25]

$$\chi / \mu = \alpha \cdot \left[\frac{(1 + 10 \cdot V_{\#}^{1/3}) \cdot (1 + 0.5 \cdot Br^{\beta})}{1 + \pi_v} \right]^{0.4} \cdot \pi_{i\#}^{0.6} , \tag{3}$$

where the empirical coefficients α and β are equal for hydrocarbon-air mixtures to α=1.75 and β=0.5 and for hydrogen-air mixtures to α=1.00 and β=0.8 and the *Bradley number* is

$$Br = \frac{F}{V^{2/3}} \cdot \frac{c_{ui}}{S_{ui}(E_i - 1)} . \tag{4}$$

Correlations (1) and (3) express different dependence of the turbulence level, which is measured in our papers by the value of the DOI number, on enclosure scale. The turbulence will increase with volume/scale according to our correlation (3) and will not change with scale in the previous correlation (1) if the Bradley number (or W_c) is constant. It has been shown previously [14] that our result (3) agrees with the conclusions of Gouldin [21] who used the fractals theory in turbulent flames modelling. Both our results of direct processing of large amount of experiments and the fractal-based approach yield the power dependence of the turbulence factor on enclosure scale with the exponent equal to about 0.4. In contrast to the earlier correlation (1), we have revealed dependence not only on the Bradley number (*venting parameter* in terminology of [20]) and the vent opening pressure but on the *enclosure scale*, $V^{1/3}$, too. This finding allowed us to improve the vent sizing technology drastically.

The influence of the vent cover release pressure on the level of turbulence is different in the correlations (1) and (3) as well. However, the size of variation of this parameter and hence its influence on the DOI number is not so important as for two other arguments - enclosure scale and the Bradley number. Since the influence of the vent release pressure on the venting generated turbulence can not be revealed unambiguously from the existing experimental data, we will leave discussion on this issue for the future.

A reasonable compliance of the DOI numbers obtained directly from processing of the data of 44 experiments in enclosures, of various volumes up to 8087 m^3 and initial pressures up to 0.7 MPa, with the DOI numbers calculated by the correlation (3) is demonstrated in Fig. 1. Noticeable experimental data scattering can be seen at high level of turbulence for large-scale experiments in "segment" form 4000 m^3 enclosure [23] and Monsanto real explosion in 8087 m^3 building [24].

The dependence of the DOI number on enclosure volume is presented in Fig. 2 for a series of the Bradley number 0.3, 3, 30, 330 and dimensionless vent opening pressure π_v=1.01 for both hydrocarbon-air and hydrogen-air mixtures. The turbulence level grows with volume of enclosure. The higher the Bradley number the higher the DOI number for a given volume of enclosure. The influence of the Bradley number is more significant for hydrogen-air mixtures.

The DOI numbers have been obtained recently [15] by processing experimental data on vented 4.8% propane-air deflagrations in a vessel of 0.65 m^3 volume [22] at atmospheric and elevated pressures up to 0.7 MPa with central ignition. It is easy to see in Fig. 3 that the suggested correlation between the DOI number and the initial pressure is reasonable. The turbulence level for vented deflagration in conditions of experiments [22] increased from 4-6 at initial atmospheric pressure to 15-20 at initial pressure 7 atmospheres. Hence the increase of initial pressure from 1 to 7 atmospheres leads to about four-fold increase of the turbulence level. It has been found that there is only 20% increase of the turbulence factor for the stage of deflagration in closed vessel for the same increase of initial pressure [25]. This result demonstrates explicitly that it is venting that is responsible for a substantial increase in the turbulence level, but not just an elevated initial pressure itself.

The correlation (3) suggested fits the experimental data regardless of the location of ignition source relatively to a vent (see Fig. 4).

CONSERVATIVE VENT SIZING

The universal correlation for vented gaseous deflagrations was obtained previously by the best fit to experimental data. However, explosion safety practitioners are used to employ techniques that are conservative from a practical point of view rather than accurate from a mathematical point of view. The conservative form of the universal correlation has been developed in this paper

$$\frac{\pi_{red}}{\pi_v^{2.5}} = 5.65 \cdot Br_t^{-2.5} \left(\frac{\pi_{red}}{\pi_v^{2.5}} \le 1; Br_t \ge 2\right) \quad \text{and} \quad \frac{\pi_{red}}{\pi_v^{2.5}} = 7.9 - 5.8 \cdot Br_t^{0.25} \left(\frac{\pi_{red}}{\pi_v^{2.5}} > 1; Br_t < 2\right), \tag{5}$$

where the turbulent Bradley number Br_t is proportional to the ratio of the Bradley number and the DOI number. The exact proportion coefficient is given in the following formula:

$$Br_t = \frac{\sqrt{E_i/\gamma_u}}{\sqrt[3]{36\pi_0}} \cdot \frac{Br}{\chi/\mu}. \tag{6}$$

The correlation estimate of the DOI number in the form (3) has been employed to design the correlation (5) and hence has to be used along with it in vent sizing. The essence of the conservative approach is to "cover" all experimental points on the graph from the top. Two curves (5) cover from the top all the processed up to date 139 tests, as shown in Fig. 5. All the data for the experimental points in Fig. 5 were taken directly from the tests processing excluding the values for the DOI numbers, which were calculated by the correlation (3) and then substituted to the turbulent Bradley number.

The correlations (3) and (5) form the base for the innovative performance-based vent sizing technology. This technology has been compared recently in our current study with the most wide spread in the world the NFPA 68 standard "Guide for venting of deflagrations". A detailed comparison between these two approaches is not the objective of this paper. However, it is useful to mention that in about 90% of cases the predictions of experimental results made using our innovative technology are more accurate. It is the demonstration that the physically sound theoretical approach can produce more robust engineering tool compared to the existing purely empirical approach.

SCALING OF VENT COVER INERTIA

The phenomenon of the double pressure peak in vented gaseous deflagration experiments has been well established since the beginning of research at 1950th, but it has not been explained on a satisfactory theoretical basis for a long time [3]. The principal works by Yao [26], Pasman et al. [27], Bradley and Mitcheson [28] proved theoretically the existence of a two-peak structure. The first theoretical work of the present author [6] in 1981, that laid the foundations for all the studies that followed, explained this phenomenon too. Later in 1986, a more complex four-peak pressure structure was revealed and investigated for cubic enclosures and very low vent release pressures [29]. Increasing the failure pressure of relief panels to 7.5 kPa was found to result in two pressure peaks becoming the dominant features of the observed pressure-time profiles [29]. Moreover, the relative ease, with which the fourth acoustically driven peak can be significantly reduced in magnitude or eliminated altogether, suggests that in most practical situations acoustically enhanced pressures will be of little or no importance [29, 30].

It seems that Cubbage and Simmonds [31] were the first who made the statement that inertia of the vent panel, at least over the range of conditions of their tests, had no effect on the second pressure peak. It was demonstrated then in 1978 by Zalosh [32], for tests in a 0.19-m^3 vessel with the same vent area but different vent release pressures, that the second peak pressures were almost identical, even though the first peaks differed by a factor of 2.5. The phenomenon has been explained theoretically in our paper [11]. This result would be expected to be correct only in those cases where the vent cover is removed fully before the completion of deflagration inside enclosure. Recent experimental results on vented deflagration in a small-scale duct with ignition at rear wall have shown that the phenomenon of independence of the second pressure peak on the first one does not work always [33]. The case of central ignition in an enclosure with the ratio of the smallest to the largest sizes less than 1:3 will be considered further in this paper. We will employ the effect of independence of the second pressure peak on the vent cover release pressure in our further calculations.

In 1973 Cubbage and Marshall suggested a formula for the maximum explosion pressure at the first pressure peak that appears after the release of the inertial vent cover [34]

$$P_1 = P_v + 0.023 \cdot S_u^2 \cdot K \cdot w / V^{1/3} . \tag{7}$$

Equation (7) is based on experiments in chambers of volumes up to 30 m^3 using a variety of fuel gases to maximize the range of burning velocity. Unlike the Cubbage and Simmonds's formula for freely lying horizontal relief panels [31], this latter correlation was devised from the experiments with relief panels that were positively fixed and had to be physically broken by the overpressure in order to create an open vent (P_v is larger than about 2 kPa). The fact that the overpressure is proportional to the square S_u^2 of the burning velocity, and not to S_u, leads to some overestimation of the explosion pressure for mixtures with S_u > 0.5 m/s. On the basis of experiments with such mixtures, British Gas [35] recommended that the coefficient in (7) should be reduced from 0.023 to 0.007. There is even an opinion that the equation (7) can be applied with confidence to empty rooms of volumes up to 200-300 m^3 [36].

Let us consider enclosures able to withstand internal overpressure not more than 1 bar. It means that the first of two equations (5) can be used to calculate the second pressure peak. Hence, for initial atmospheric pressure we can write

$$P_2 = 1 + 5.65 \cdot P_v^{2.5} \cdot Br_t^{-2.5} \, (Br_t \geq 2) . \tag{8}$$

For the overpressure at the second peak to be less than 1 bar the turbulent Bradley number has to be equal to 2 or greater.

Russian scientists Korotkikh and Baratov concluded more than 20 years ago that the cause of building destruction by internal gaseous deflagration is in most cases not the insufficient vent area but the excessive inertia of removing elements [37]. Since that time there has been no suggestions of a reasonable relationship that would calculate the upper limit of the cover inertia dependent on the enclosure volume, the vent size, the mixture characteristics and the venting generated turbulence.

It can be stated that for a cost-efficiently designed explosion protection system, when the vent area is equal to its lower limit and the inertia may be equal to its upper limit, the first pressure peak value has to be equal to or less than the second peak value. The upper limit for the inertia of a vent cover can be derived from this assumption after simple calculations with equations (7) and (8) and presented in the form

$$w \le \frac{V^{1/3} \cdot (F / A_{cs})}{0.023 \cdot S_u^2 \cdot \chi^2 \cdot} \left[1 + P_v \left(\frac{5.65 \cdot (36 \cdot \pi_o)^{5/6}}{Br^{2.5} \cdot (E_i / \gamma_u)^{5/4}} \cdot (\chi / \mu)^{2.5} \cdot P_v^{1.5} - 1 \right) \right], \tag{9}$$

where the burning velocity, S_u, has been multiplied on the turbulence factor, χ, as a conservative measure.

Let us calculate the upper limits for the vent cover inertia of enclosures of different volume of 0.1, 10, 100, and 1000 m^3 for the following model conditions and at the assumption that formula (7) is valid for cases under consideration. Let us suggest for simplicity that enclosures have a cubical form, and a relief panel is mounted in one side only and has an area enough to ensure the reduced pressure 30 kPa. Let us assume further that for all enclosures the vent cover release pressure is equal to P_v=1.03 bar and near-stoichiometric propane-air mixture is used as a fuel (S_u=0.31 m/s; E_i=7.9; γ_u=1.365; c_{ui}=335 m/s). These values of reduced pressure and vent release pressure have been used to determine the value of the turbulent Bradley number Br_t=3.4 by the first of the two equations of the universal correlation (5). The turbulent Bradley number is the same for all cases. The DOI numbers were calculated by the correlation (3). The values for the turbulence factor were calculated from the DOI numbers with a characteristic value of the discharge coefficient μ=0.6 for all enclosures. The values of the vent areas F, and hence the respective ratios F/A_{cs}, were calculated by employment of the correlations (5) and (3) of the conservative vent sizing technology. General initial and intermediate data and the results of calculation of the upper limit of the vent cover inertia for different enclosures are given in Table 1.

Table 1. General initial, intermediate data and upper limits for vent cover inertia.

V, m^3	F, m^2	F/A_{cs}	Br	χ/μ	χ	**w, kg/m^2**
0.1	0.04	0.20	31	4.5	2.7	**< 0.31**
10	1.76	0.38	59	8.6	5.2	**< 16**
100	11.62	0.54	84	12.3	7.4	**< 113**
1000	77.70	0.78	122	17.7	10.6	**< 782**

It is easy to see that the upper limits of the vent cover inertia depend significantly on the enclosure volumes. Nevertheless all vent covers are of 100% “efficiency”. The same material can be “heavy” for small-scale enclosure and “light” for large-scale ones. For example, the density of glass is about 2470–2560 kg/m^3 and hence the inertia of panes with thickness in the range of 2–5 mm constitutes 5–13 kg/m^2. Such inertia practically has no influence on the value of the first pressure peak in empty room-size enclosures and enclosures of bigger volume without initial turbulence. However, glass is unacceptable for use as a vent cover for enclosures with volume of about 0.1 m^3 under the conditions considered in this paper. On the other hand, we have obtained that the upper limit of the inertia for the vent covers in large-scale enclosures is about 800 kg/m^2 even if the estimate is conservative. This value is well above those from 0.5 to 20 kg/m^2 that are under discussions for implementation into international standards.

CONCLUSIONS

The correlation for venting generated turbulence, i.e. the DOI number, is presented and discussed in detail. It is an essential part of the innovative performance-based vent sizing technology, which predicts experimental data on vented gaseous deflagrations with better accuracy than other vent sizing techniques, including the NFPA 68 standard. The conservative form of the universal correlation for vented gaseous deflagrations is presented for the first time.

The issue of scaling of vent cover inertia is analysed. The equation to calculate the upper limit for inertia of vent cover is suggested for the first time. It takes into account the dependence of the venting generated turbulence on the conditions of vented gaseous deflagration, such as the scale of enclosure and the Bradley number. Conservative estimations have shown that the upper limit for vent cover inertia is reaching 113 kg/m^2 for 100 m^3 enclosure and 782 kg/m^2 for 1000 m^3 enclosure (the case of propane-air mixture and reduced pressure of 30 kPa is considered). This is substantially higher than the values under consideration for implementation into international standards. The equation suggested has to be verified further against experimental data and can be used in the future as a part of performance-based vent sizing technology.

NOMENCLATURE

A	characteristic enclosure size, m
A_{cs}	cross section area of enclosure which is parallel to a wall with relief panel, m^2
Br	Bradley number
Br_t	turbulent Bradley number
c_{ui}	speed of sound at initial conditions of deflagration, m/s
E_i	combustion products expansion coefficient at initial conditions
F	vent area, m^2
K	vent area coefficient (ratio of the area of enclosure cross section to the area of relief)
P_1, P_2	values of the first and the second pressure peaks, bar abs.
P_i	initial pressure, bar abs. = 10^5 Pa
P_{max}	maximum explosion pressure at the second pressure peak, bar abs. = 10^5 Pa
P_{red}	reduced pressure, bar gauge = 10^5 Pa, $P_{red} = (P_{max} - P_i)$
P_{stat}	vent closure release pressure used in the NFPA 68, bar gauge = 10^5 Pa, $P_{stat} = P_v - P_i$
P_v	vent closure release pressure, bar abs. = 10^5 Pa
S_u	laminar burning velocity, m/s
S_{ui}	burning velocity at initial conditions, m/s
V	enclosure volume, m^3
$V_\#$	dimensionless volume (numerically equal to enclosure volume in m^3), $V_\# = V / V_{1m^3}$
w	inertia of vent cover, kg/m^2
W_C	venting parameter by Crescitelli et al.
Greek	
α, β	empirical coefficients

γ_u, γ specific heats ratio for unburned mixture
μ generalized discharge coefficient
π_{red} dimensionless maximum explosion overpressure (reduced pressure), $\pi_{red} = P_{red} / P_i$
π_v dimensionless vent closure release pressure, $\pi_v = P_v / P_i = (P_{stat} / P_i + 1)$
$\pi_{i\#}$ dimensionless initial pressure, $\pi_{i\#} = (P_i / 1bar)$
π_0 "pi" number, π_0=3.14
χ turbulence factor after vent opening
χ_0 turbulence factor before vent opening
χ/μ deflagration-outflow interaction number (the DOI number)
$\chi / \mu_{\exp}$ the DOI number, determined by processing experimental data

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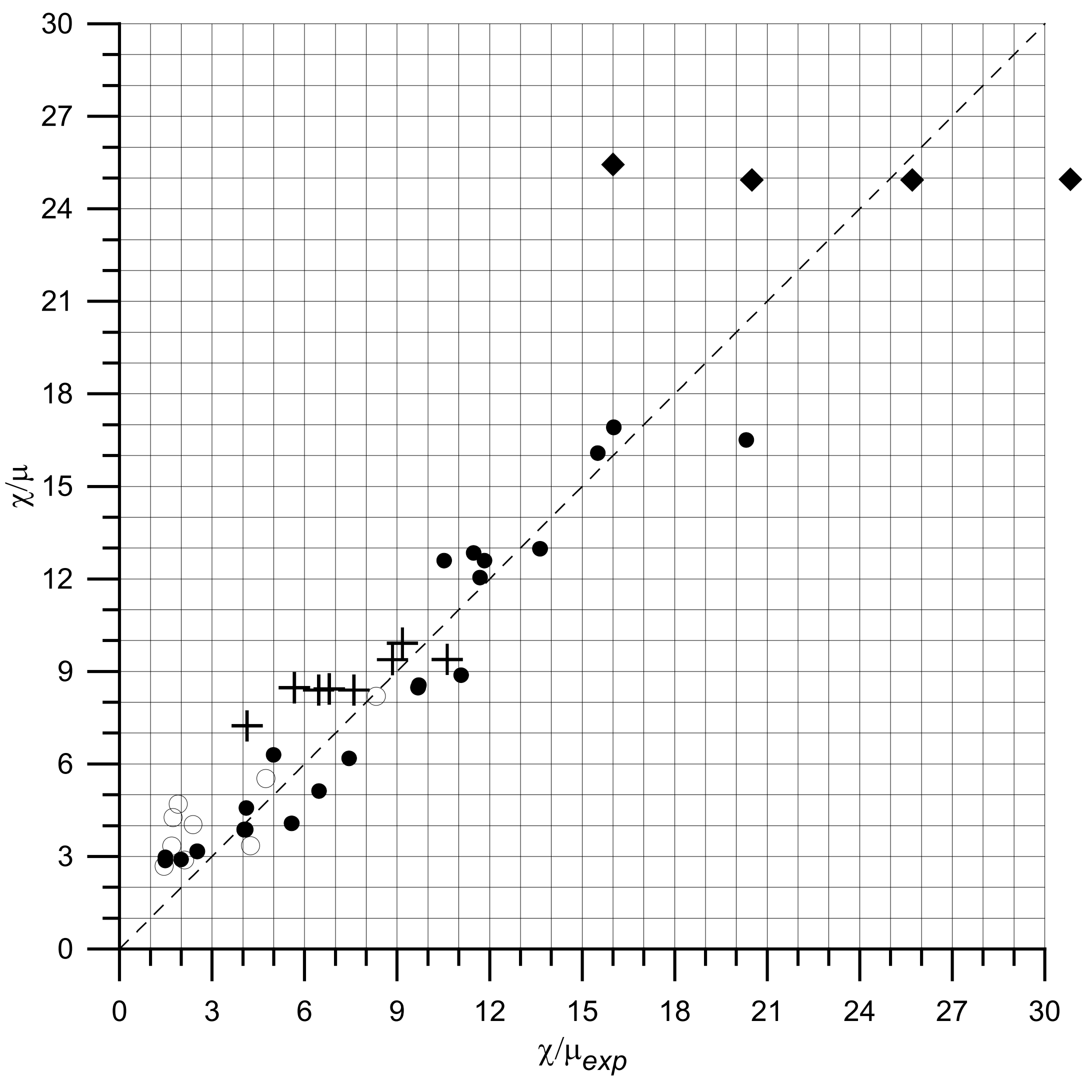

Figure 1. The DOI numbers obtained by processing experimental data, χ/μ_{exp}, and determined by correlation (3), χ/μ, for enclosures of different volume: black circles - 0.02-1.00 m^3 (including experiments at elevated initial pressures up to 7 bar [22]); white circles – 2-11 m^3; crosses – 30-50 m^3; diamonds – 4000-8087 m^3.

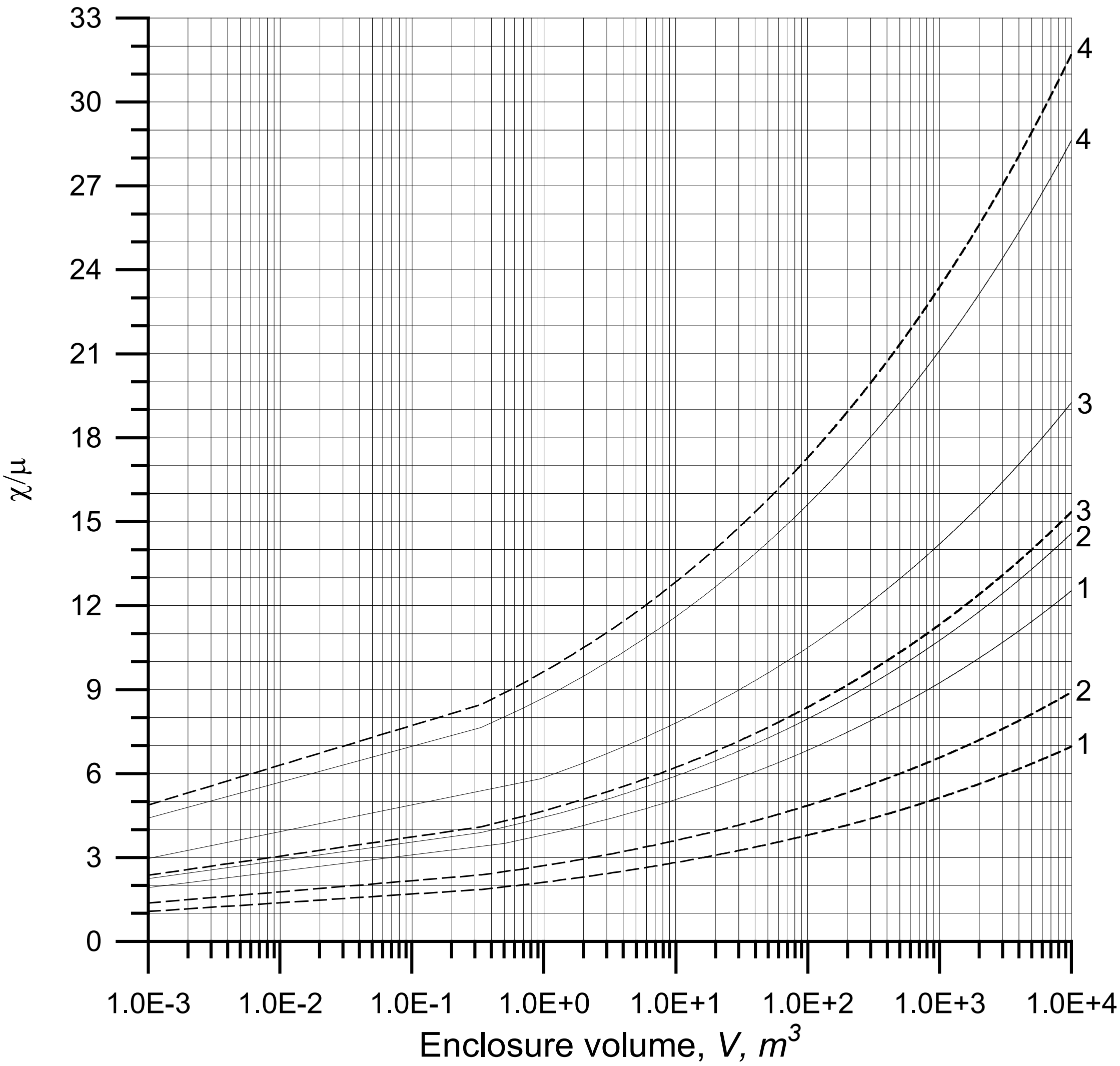

Figure 2. The dependence of the DOI numbers on enclosure volume for hydrocarbon-air (solid lines) and hydrogen-air (dashed lines) mixtures for dimensionless vent cover release pressure π_v =1.01 and the Bradley number 0.3 (curves 1), 3 (curves 2), 30 (curves 3) and 330 (curves 4).

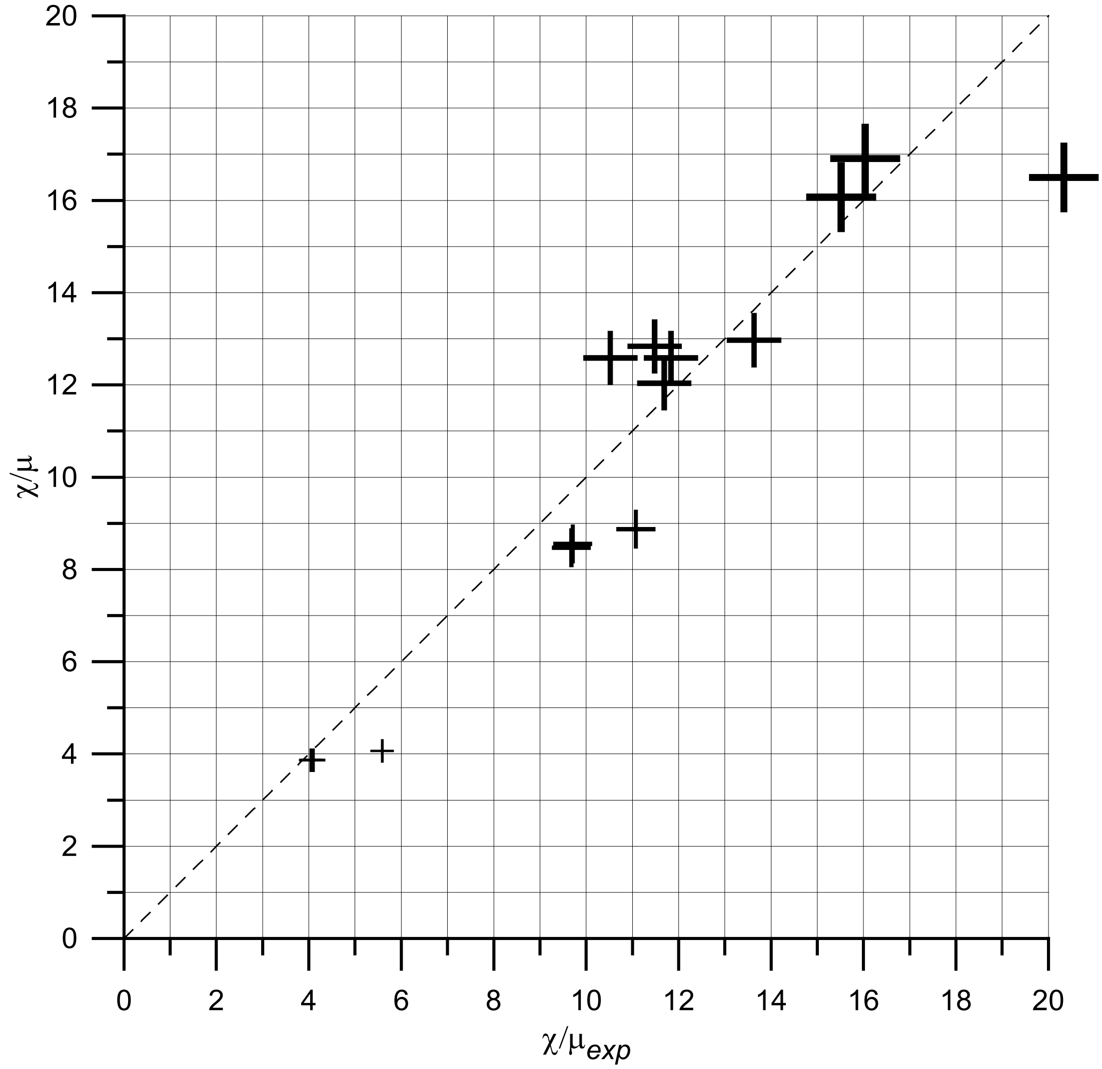

Figure 3. The DOI numbers obtained by processing experimental data [22] at atmospheric and elevated pressures, $\chi/\mu_{\rm exp}$, and determined by correlation (3), χ/μ: four series of tests with initial pressures 1, 3, 5 and 7 bar (the higher initial pressure the bigger cross size on the graph).

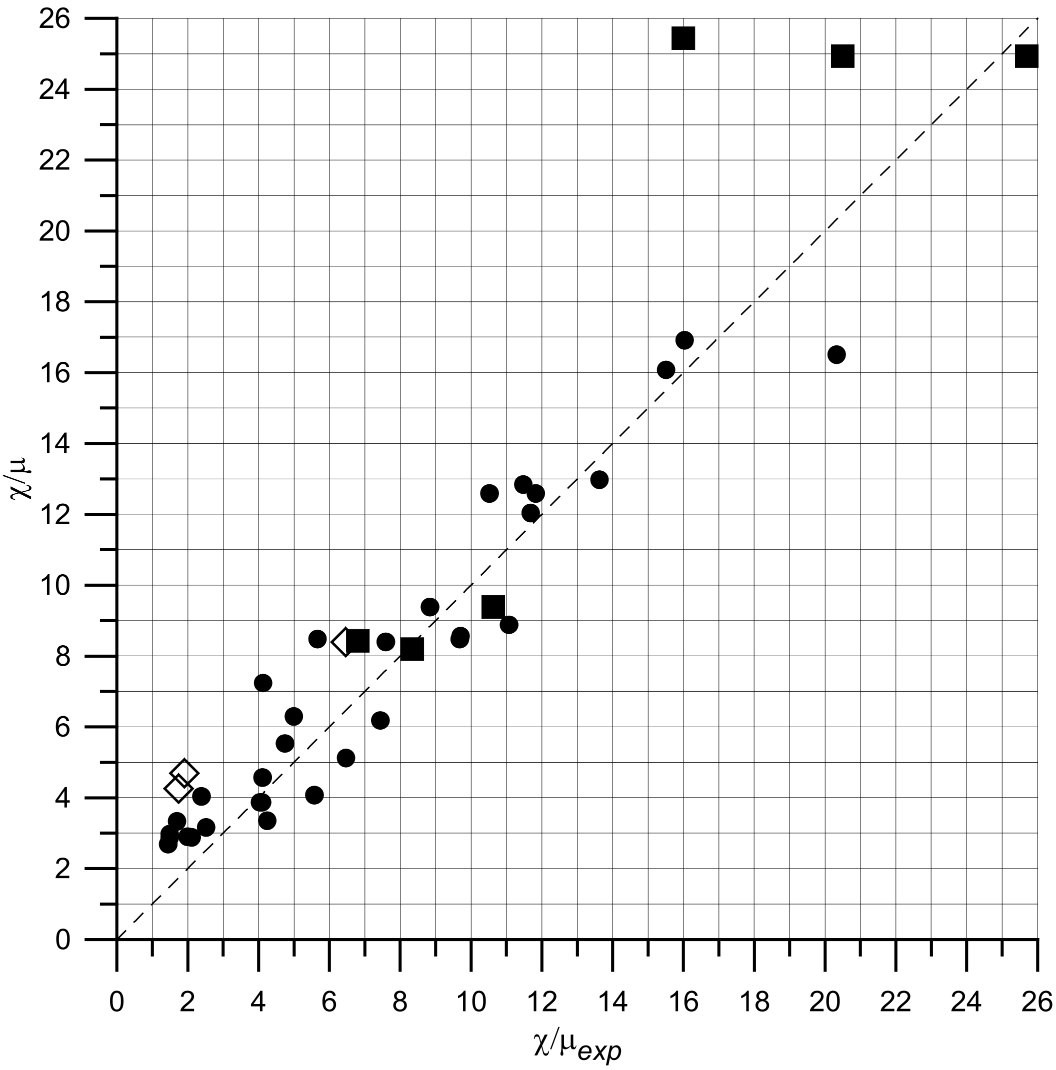

Figure 4. The DOI numbers obtained by processing experimental data, χ/μ_{exp}, and determined by correlation (3), χ/μ, for different locations of ignition source: black circles – centre; black squares – rear wall; white diamonds – near the vent.

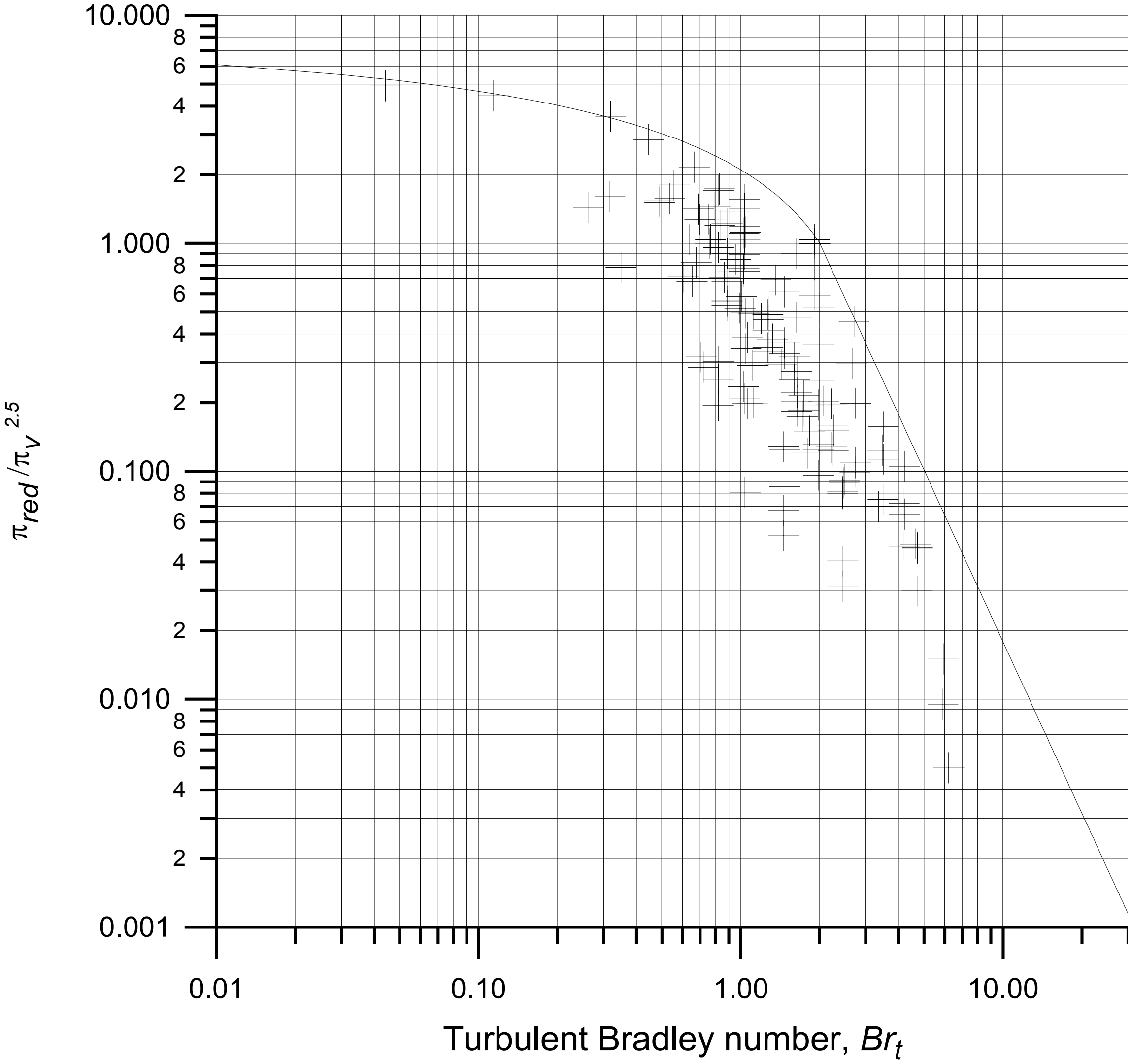

Figure 5. The conservative form of the universal correlation (solid curve) and 139 experimental points (crosses).