



Detailed Modeling of Flame Jets and Flares



G. Avery, "Natural gas flaring draws scrutiny in Colorado as regulators seek to limit emissions", Denver Business Journal, Oct 23, 2020.

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Detailed Modeling of Flame Jets and Flares

Process Safety and Risk Management Practices

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1 Introduction

Widely used methods for calculating thermal radiation impacts of flares and/or flame jets rely on point source, multiple point source, integral, or solid flame representations of the flame surface. The flame jet length and flame centerline geometry are established by calculating the curvilinear distance to the lower flammable limits without combustion. Some computational fluid dynamics models have attempted to include combustion reactions modeling, but such methods are still not practical for most design and risk applications.

In general, current methods generate adequate thermal radiation estimates in the far field because at distances far away from the flame, the flame will appear as a point source. In the near field, the flame geometry becomes more important. As more air is entrained into the flame, the flow momentum influence on flame geometry is reduced and the influence of buoyancy due to combustion on flame geometry dominates. This is especially important for horizontal releases and/or releases with light materials such as hydrogen.

Several types of models are commonly used to assess the thermal radiation hazards of flame jets and flares. These models are generally divided into three categories: (a) point source or line source models, (b) solid flame models, and (c) computational fluid dynamics (CFD) models. A line source model is made up of multiple point sources. A solid flame model represents the flame and flame surfaces using an idealized geometry to obtain incident thermal radiation levels at locations of interest.

The most practical flame jet and flare models are multiple point source models. They rely on jet dispersion to first establish the flame jet length and curvilinear path and then integrate the multiple point source energy release rates along the entire flame jet path.

A detailed multiple point source model for the calculation of thermal radiation from flame jets and flares using SuperChems^(R) Expert is described in detail. While the fraction of flame jet combustion energy available for radiation is based on Shore's [3] method or can be based on measured data, detailed combustion equilibrium calculations are also integrated to yield combustion species identities, compositions, flame temperature, as well as conversion and destruction efficiencies.

User tools are also provided by SuperChems[®] Expert for the assessment of thermal radiation on nearby structures as well as the calculation of gas purge rates, and visualization of noise and thermal radiation contours with options for sound power level weighting and combustion spectrum efficiency selection. Thermal radiation and/or noise contours from one or more sources/flares can be combined and displayed on site maps.

Figure 1: A hydrocarbons pool fire



Source: [2]

Table 1: Qualitative temperature assessment of visible flames

Color	Temperature, °C
Faint red glow	≈ 480
Dark red	≈ 580
Bright red but slightly orange	≈ 730
Bright orange	≈ 930
Pale yellowish orange	$\approx 1,100$
Yellowish white	$\approx 1,300$
White (Yellowish if seen from a distance through the atmosphere)	$> 1,400$

2 The Complex Nature of Soot

The luminosity of hydrocarbon fires is mainly due to soot formation during the combustion process [4]. For example, when liquid hydrocarbons burn in a pool fire, the liquid is vaporized by heating from the flame and burns near the liquid pool surface at the upper flammability limit or vapor fuel rich conditions. Under fuel rich conditions, soot is formed. Soot is different from carbon such as graphite or fullerenes that have highly ordered structures.

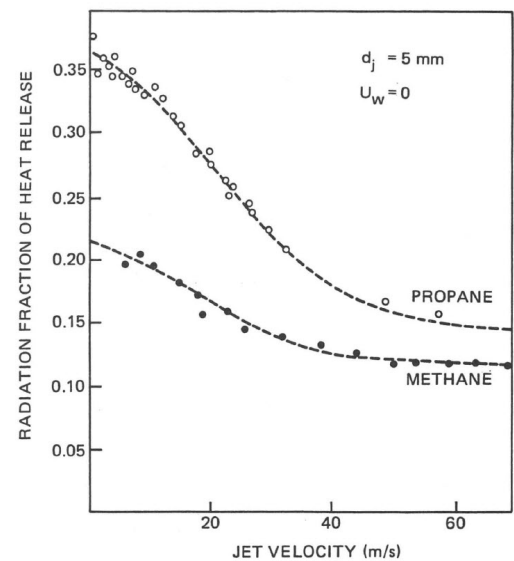
Soot results from the incomplete combustion of hydrocarbons. Soot is a black, powdery substance made of carbon particles, formed from the incomplete combustion of fuels like oil, gas, or wood when there is not enough oxygen or heat for all the carbon to turn into CO_2 or CO . Soot is unburnt carbon that forms through complex chemical reactions involving polycyclic aromatic hydrocarbons and stabilizes into solid particles. Soot is considered a hazardous substance with carcinogenic properties.

Hot soot within the flame incandesces and creates an orange glow. Bright yellow and orange colors are characteristics of hot soot particles. The hotter the soot the more white the flame looks. Depending on the color of the flame surface, one can deduce the flame surface temperature as illustrated in Table 1.

In a pool fire, as the soot rises away from the liquid pool surface it cools, and without enough air to continue burning it turns black. Soot is the main component of smoke. If not completely burned, residual soot is released as smoke. This is the primary reason large pool fires and pool fires of heavy hydrocarbons create black smoke away from the burning pool surface (see Figure 1).

The light emitted from soot extends across the visible wavelength range and into the near-infrared range. Cool soot looks black, but while it is in the flame or

Figure 2: Effect of jet velocity on radiated fraction of combustion energy



Source: [5]

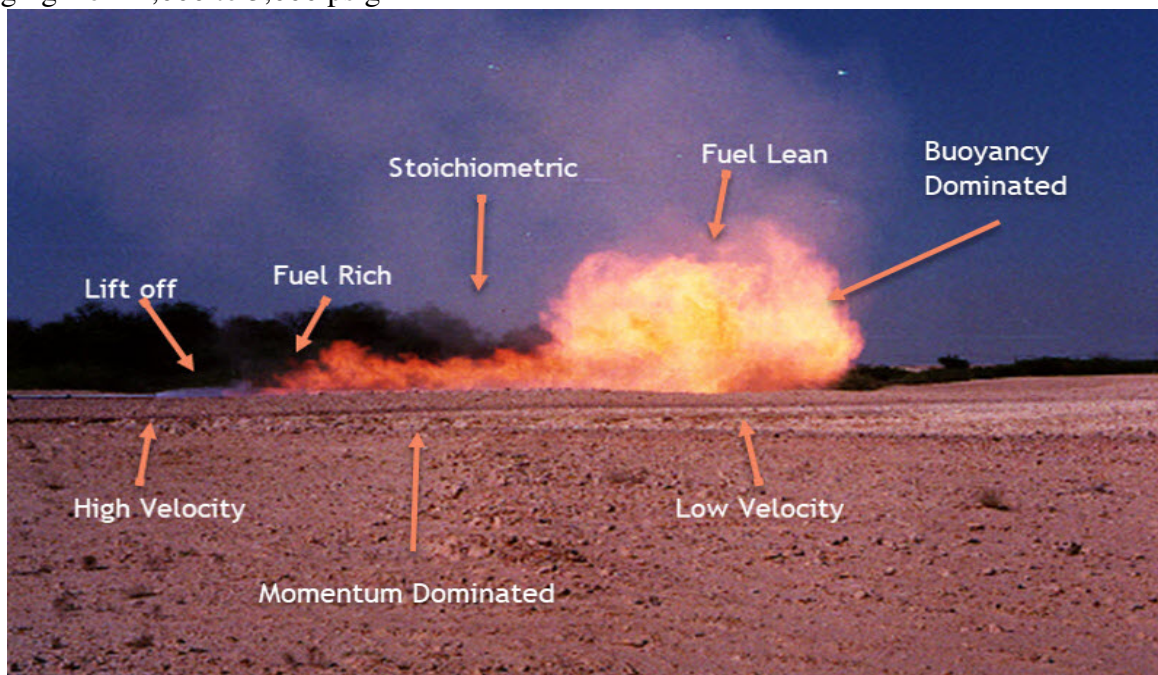
fire, the heat liberated from nearby combustion reactions makes the soot particles glow like tiny light bulbs filaments [4]. The solid nature of soot gives most flames their luminosity and as a result their thermal radiation emissive power. Thermal radiation heating from the flame surface is fundamentally controlled by the production of soot and smoke. Hot soot particles dominate thermal radiation and cold soot particles insulate the surroundings from thermal radiation. Thus, soot is the giver and taker of light [4]. Cold soot is essentially a blackbody with an emissivity of 1. Soot particles have great abilities to both absorb and emit light or thermal radiation.

A flame or fire without soot produces a dim glow. This is typical of hydrogen fires and methanol fires where soot is either not produced or produced in very small quantities.

3 Soot Formation in Flame Jets and Flares

Unlike pool fires, where the flame above the pool liquid surface occurs at the upper flammability limit, i.e. under fuel rich conditions where soot is formed, flame jets and flares burn more efficiently as the momentum and buoyancy of the flame jet or flare entrains air at the perimeter of the flowing jet and causes more complete combustion due to turbulent mixing (see Figure 3).

Figure 3: A high pressure horizontal flame jet flowing from a 4 inch pipe at a gas wellhead pressure ranging from 2,000 to 3,000 psig



Source: [ioMosaic®](#)

We do not see from Figure 3 black smoke formation because of near complete combustion of the flowing gas from the wellhead. Closer to the wellhead discharge pipe, lift off occurs and combustion does not occur due to the high exit velocity of the jet.

Combustion in the flame jet starts at fuel rich conditions, then stoichiometric conditions, and ends with fuel lean conditions. Soot is being produced but continues to be burned and does not cool enough to cause black smoke. Towards the end of the flame jet we note that hot combustion products start to influence the trajectory of the flame jet due to competing buoyancy and momentum forces as the flame jet velocity decreases due to entrainment of air.

These observations lead us to conclude that the fraction of energy radiated by flame jets and flares will decrease with increasing jet velocity (also see Figure 2). Higher jet velocities lead to more air entrainment and turbulent mixing, i.e. more efficient combustion. Increased turbulence and mixing (due to higher jet velocity) leads to (a) faster oxidation of soot precursors and a decrease in the overall production of soot within the flame, (b) less residence time of combustion products in the hotter temperature zones further limiting soot formation and growth, and as a result a less luminous flames, (c) lower peak flame temperature by distributing the the heat release over a larger flame area, and (d) flame geometry changes as higher velocity jets are longer and narrower.

The overall fraction of combustion energy radiated can be related to the flame jet path integrated average flame temperature ¹, T_f :

$$\epsilon \sigma T_f^4 = \frac{\chi_r \dot{M} \Delta H_c}{A_f} \quad (1)$$

where ϵ is the flame emissivity, χ_r is the overall fraction of combustion energy radiated, $\dot{M}$ is the mass flow rate of fuel in kg/s, ΔH_c is the heat of combustion of the fuel in J/kg, A_f is the overall surface area of the flame in m², and $\sigma = 5.67 \times 10^{-8} \text{ W}/(\text{m}^2\text{K}^4)$ is the Stefan-Boltzmann constant.

We note that the rate of air entrainment into the jet is proportional to the difference between the jet velocity and air velocity:

$$\text{Rate of air entrainment} \propto 2\pi r_j (u_j - u_a) \quad (2)$$

where r_j is the jet radius, u_j is the jet velocity, and u_a is the air velocity. However, the mass flow rate of the jet is proportional to the flow area of the jet:

$$\text{Mass flow rate} \propto \pi r_j^2 \quad (3)$$

As a result, large flow diameter flame jets create larger cores of fuel close to the discharge point with less mixing of oxidant (ambient air) which causes a higher fraction of the combustion energy to be radiated due to rich combustion conditions. This is also evident from a correlation developed by Shore [3] for the fraction of energy radiated from flame jets and flares (see Equation 5).

$$^1T_f = \frac{\int_{\text{LFL}}^{\text{UFL}} T dx}{\text{UFL} - \text{LFL}}$$

Figure 4: Multiple point source or line source model

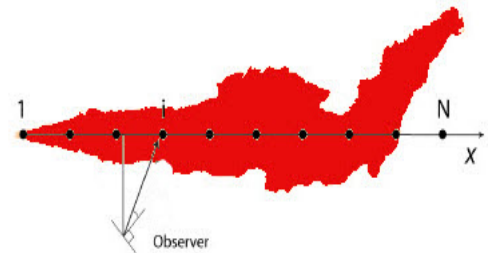


Table 2: Heat from various gaseous flames

Gas Name	Flare Tip Diameter (in)	Fraction Radiated
Hydrogen	< 1	0.10
	1.6	0.11
	3.3	0.16
	8.0	0.15
	16.0	0.17
Butane	< 1	0.29
	1.6	0.29
	3.3	0.29
	8.0	0.28
	16.0	0.30
Methane	< 1	0.16
	1.6	0.16
	3.3	0.15
Natural Gas	8.0	0.19
	16.0	0.23

Source: [EPA Economic and Cost Analysis for Air Pollution Regulations](#). Last accessed August 12, 2026.

4 Radiated Fraction of Combustion Energy

Shore [3] presents the following equation for the estimation of the energy fraction radiated from a flare:

$$\chi_r = 0.075 F_i \exp \left(GP - \frac{1}{\sqrt{D}} \right) \quad (4)$$

$$G = \left(\frac{c_o}{u_o} \right)^{0.2} \quad (5)$$

$$P = \frac{12C + 24S - 3O + 3OH}{M_w} \quad (6)$$

$F_i = 1.00$ For low probability of design relief

$= 1.15$ For high probability of design relief

where D is the flare tip diameter in (inches), c_o is the sonic velocity of the discharged gas (m/s), u_o is the discharge velocity of gas from the flare tip (m/s), G is a turbulence factor, and P is the pseudo fraction of solids in the discharged gas stream. C , S , O and OH are the mole fractions of carbon, sulfur, oxygen and OH atoms in the discharged gas molecule(s).

Table 2 illustrates typical radiation fraction values used for flares ².

[SuperChems® Expert](#) evaluates the fraction radiated using Equation 4, but the user can override this option and enter a specific fraction radiated value. A user defined value of 0.3 is conservatively

²<https://www.epa.gov/economic-and-cost-analysis-air-pollution-regulations/chapter-1-flares>

high for hydrocarbon flames.

5 Multiple Point Source Models

A natural extension of the point source thermal radiation model (see Equation 7) is its application to flame jets and flares. Assuming a flame jet shape, the total radiation intensity of the jet can be evaluated using a point source located at the center of the jet or by using multiple point sources distributed along the flame jet or flare path as shown in Figures 4 and 5.

The incident thermal flux at any location, $I(x_t, y_t, z_t)$, can be obtained by integrating the following expression over the entire flame curvilinear path:

$$I(x_t, y_t, z_t) = \int_0^{L_f} \underbrace{\frac{\tau \cos \theta}{4\pi s^2}}_{\text{Point Source}} E dl \quad (7)$$

$$E = \frac{\chi_r \dot{M} \Delta H_c}{L_f} \quad (8)$$

$$s^2 = (z_t - z)^2 + y_t^2 + (x_t - x)^2 \quad (9)$$

where E is in W/m which represents the combustion energy generation rate divided over the entire flame curvilinear length L_f , s is the distance in m from the point source to target surface receiving the incident flux, θ is the angle between normal to the surface and line of sight from the point source in degrees, τ is the atmospheric transmissivity which depends on s and atmospheric conditions, χ_r is the fraction of total combustion energy radiated by the flame in all directions to the surroundings, ΔH_c is the total heat of combustion in J/kg, $\dot{M}$ is the mass flow rate in kg/s, x_t , y_t , and z_t are the Cartesian coordinates of the target surface receiving thermal radiation, and x , y , and z are the Cartesian coordinates of the point source along the curvilinear path of the flame jet. If the point source is located at coordinates $(x, 0, z)$, then $\cos \theta$ can be evaluated as follows:

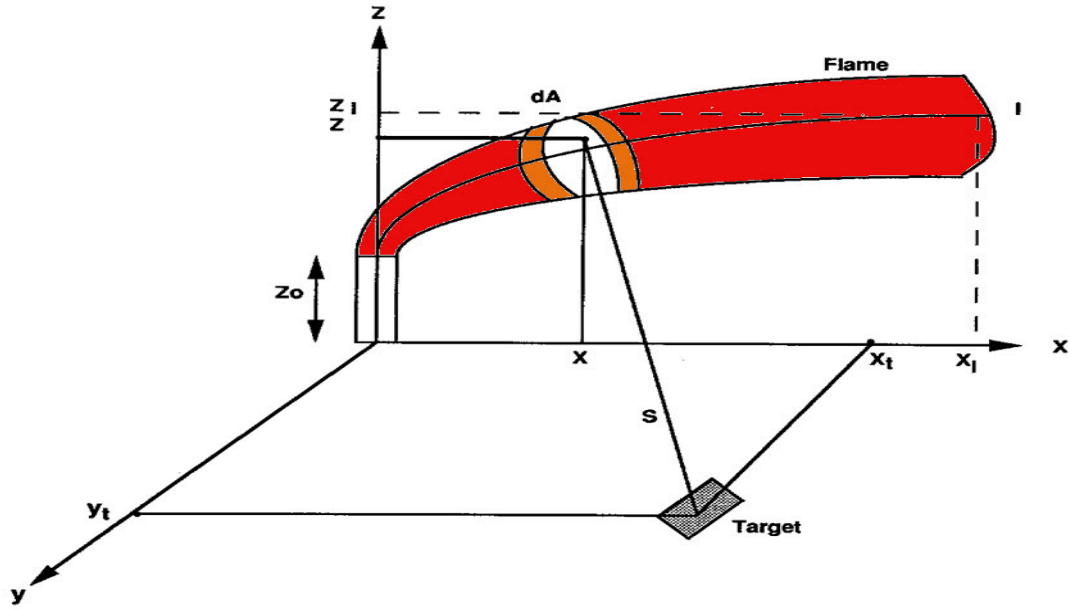
$$\cos \theta = \frac{z - z_t}{s} \text{ for } z_t < z \text{ Horizontal target} \quad (10)$$

$$\cos \theta = \frac{x_t - x}{s} \text{ for } x_t > x \text{ Vertical target perpendicular to the x-axis} \quad (11)$$

$$\cos \theta = \frac{y_t}{s} \text{ Vertical target parallel to the x-axis} \quad (12)$$

A multiple point source representation of the flame jet provides a better estimate of the incident flux in both the near field (close to the flame jet) and in the far field (far away from the flame jet). A single point source located at the center of the flame jet (as is widely used because of its simplicity) can underestimate thermal radiation levels close to the source. At large distances, the flame jet will appear as a single point source to the target, and as a result, thermal radiation levels at large distances are more reasonably predicted by a single point source model. We note from Equation 7 that the incident flux is inversely proportional to the square of the target direct line of

Figure 5: Multiple point source model for flame jets and flares



Source: [ioMosaic®](#)

sight distance ($\propto \frac{1}{s^2}$) from the point source which is why thermal radiation hazards of fires are almost always localized to the immediate surroundings of the release location.

6 Computational Fluid Dynamics Models

Many attempts have been made to solve the nonlinear, coupled, differential conservation equations using finite difference techniques. Patankar et. al. [6] solved these equations for the three-dimensional velocity distribution of a turbulent diffusion jet in a cross flow. They assumed the field to be steady and the jet fluid to be incompressible. For uniform density, the continuity equation reduces to:

$$\frac{\partial u_i}{\partial x_i} = 0 \quad (13)$$

which says simply that the divergence of the velocity field is zero. Similarly, the steady-state momentum equation for an incompressible fluid is written as:

$$\rho u_i \frac{\partial u_j}{\partial x_i} = -\frac{\partial P}{\partial x_j} + \frac{\partial \tau_{ij}}{\partial x_i} \quad (14)$$

where the u_i 's are the time-averaged velocity components of the jet fluid, P is the pressure and τ_{ij} is the tensor representing the shear stress arising from turbulent diffusion. The stresses arising from laminar diffusion are considered to be negligible by comparison.

Whenever turbulence is modeled using time-averaged conservation equations, one is immediately

faced with the problem of closure. Patankar et. al. [6] used the two-equation $k - \epsilon$ turbulence model of Launder and Spalding [7] to estimate the turbulent stress tensor of Equation 14 by means of a scalar turbulent viscosity. In this model, the turbulent stresses are related to the time-averaged rate of strain tensor according to:

$$\tau_{ij} = \mu_t \left(\frac{\partial u_i}{\partial x_j} + \frac{\partial u_j}{\partial x_i} \right) - \frac{2}{3} \rho k \delta_{ij} \quad (15)$$

where δ_{ij} is the kronecker delta, k is the turbulence kinetic energy and μ_t is the turbulent viscosity. The turbulent viscosity depends on the turbulence kinetic energy and another parameter ϵ , defined as the rate of dissipation of the turbulence kinetic energy according to:

$$\mu_t = c_\mu \rho \frac{k^2}{\epsilon} \quad (16)$$

where c_μ is a constant. The two parameters, k and ϵ , in Equation 16 are governed by the transport equations:

$$\rho u_i \frac{\partial k}{\partial x_i} = \frac{\partial}{\partial x_i} \left(\frac{\mu_t}{\sigma_k} \frac{\partial k}{\partial x_i} \right) + G - \rho \epsilon \quad (17)$$

and

$$\rho u_i \frac{\partial \epsilon}{\partial x_i} = \frac{\partial}{\partial x_i} \left(\frac{\mu_t}{\sigma_\epsilon} \frac{\partial \epsilon}{\partial x_i} \right) - \frac{\epsilon}{k} (c_1 G - c_2 \rho \epsilon) \quad (18)$$

where σ_k and σ_ϵ are constants. The quantity G in Equations 17 and 18 represents the rate of generation of turbulence kinetic energy due to velocity gradients and is given by:

$$G = \mu_t \left(\frac{\partial u_i}{\partial x_j} + \frac{\partial u_j}{\partial x_i} \right) \frac{\partial u_i}{\partial x_j} \quad (19)$$

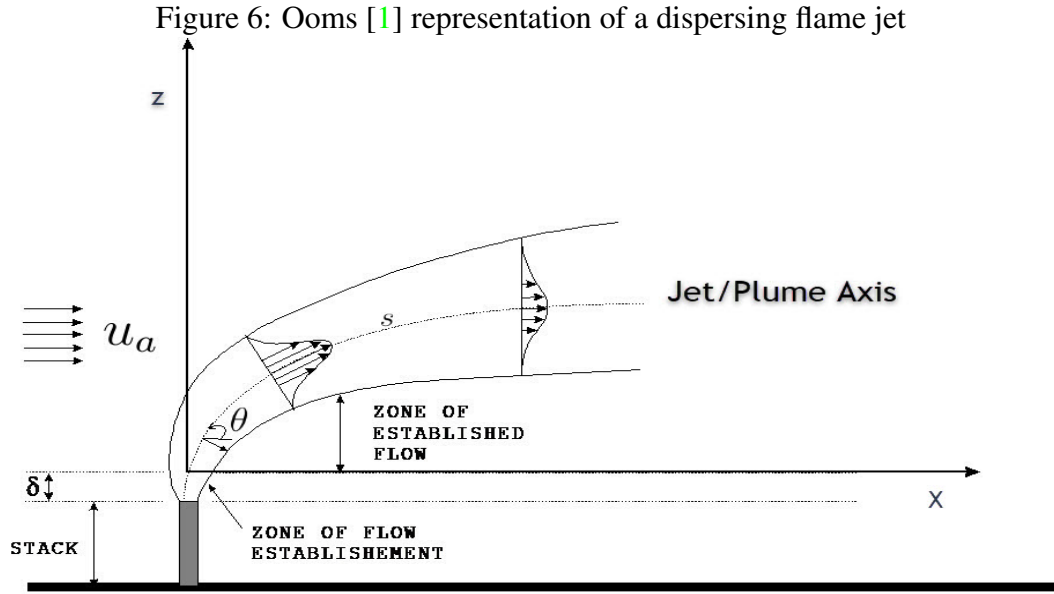
The numerical solution employed a combination of a central and upwind finite difference scheme to solve the five coupled partial differential equations given above. The results used to predict the deflected shape of the jet in the main stream. In their paper, Patankar et. al. [6] compare their results with the experimental findings of various investigators.

7 Integral 2D Equations with Similarity Profiles

Single and multiphase jet dispersion calculations can be performed using three dimensional (3D) computational fluid dynamics (CFD). However, such calculations are still computationally expensive and not practical for risk assessment and consequence modeling studies where large number of release scenarios need to be evaluated. Simplified two dimensional (2D) integral jet dispersion models are used instead because they provide high quality estimates very quickly.

Such integral models typically consist of simplified balance conservation equations for mass, momentum, and energy with Gaussian similarity profiles for velocity, density, temperature, and concentration (see Ooms [1] and Benteboula et al. [8]) as illustrated in Figure 6.

For this class of models, the flame jet or flare curvilinear distance is calculated using a modified Ooms [1] integral jet equations with similarity profiles to account for buoyancy and combustion.



Source: ioMosaic®

The profiles of density, velocity, and concentration are assumed to be symmetric and of Gaussian shape.

Unobstructed gas releases that are dominated by momentum (high pressure) and/or buoyancy can result in significant dilution by air entrainment close to the source (in the near field) due to the difference in velocity and/or buoyancy between the released gas and the surrounding atmosphere. Although this initial dilution does not significantly impact toxic dispersion hazard zone estimates in the far field (low ppm levels), it can be very important for high pressure releases in the near field where flammability and explosion hazards are a concern (high ppm levels).

7.1 Similarity Profiles

Figure 6 shows the coordinate system originally used by Ooms [1] in his formulation of the simplified integral jet (plume) dispersion equations. As shown in Figure 6, Ooms divides the jet path into two regimes: (1) the zone of flow establishment and (2) the zone of established flow. The profiles of density, velocity, and gas concentration are assumed to be symmetric and of Gaussian shape. Note that the similarity profiles hold only for the zone of established flow. These similarity profiles are given by:

$$u(s, r, \theta) = u_a \cos \theta + u^*(s) e^{-\frac{r^2}{b^2(s)}} \quad (20)$$

$$\rho(s, r, \theta) = \rho_a + \rho^*(s) e^{-\frac{r^2}{\lambda^2 b^2(s)}} \quad (21)$$

$$c(s, r, \theta) = c^*(s) e^{-\frac{r^2}{\lambda^2 b^2(s)}} \quad (22)$$

$$T(s, r, \theta) = T_a + T^*(s) e^{-\frac{r^2}{\lambda^2 b^2(s)}} \quad (23)$$

where s represents the curvilinear distance along the plume axis in m, b is the jet radius in m, r the radial distance to the plume axis in m in a normal section of the plume or radial coordinate, θ is the angle between the plume axis and the horizontal in degrees, u_a is the wind speed at the centerline elevation of the jet in m/s, ρ_a is the ambient air mass density at the centerline elevation of jet in kg/m³, T_a is the atmospheric temperature at the centerline elevation of the jet in K, u is the jet velocity in m/s, ρ is the jet mass density in kg/m³, c is the concentration of the dispersing chemical in the jet in kg/m³, T is the jet temperature in K, and the exponent * refers to centerline values at $r = 0$. λ is a turbulent Schmidt number correction adjusting the width of density, concentration, and temperature profiles relative to velocity profiles since mass/heat often diffuse at a slightly different rate than momentum.

Most flame jet equations assume that the flame jet is released in the wind direction or against the wind direction. Several correlations and models have appeared that consider flame jets issuing into a cross-wind [8]. The similarity profiles provided by Equations 20, 21, 22, and 23 can be used with x, y, and z momentum equations to account for the cross-wind impact on jet trajectory and behavior [8].

For jets issuing into a cross-wind, Equation 20 can be modified to account for the impact of cross-wind on jet trajectory and behavior:

$$u(s, r, \theta) = u_a \cos \theta \cos \sigma + u^*(s) e^{-\frac{r^2}{b^2(s)}} \quad (24)$$

where σ is the deviation angle from the cross-wind in degrees.

7.2 Air Entrainment

Air is entrained in the jet/plume as it flows through the atmosphere and is accounted for by the following three mechanisms:

1. When the velocity of the jet is much larger than that of air, the entrainment is assumed to be that of a free turbulent jet.
2. When the velocity of the plume is approximately equal to that of air, the entrainment is described as that of a cylindrical thermal plume in a stagnant atmosphere.
3. Turbulence of the atmosphere is also an important factor in calculating the amount of air entrained in the plume.

These three mechanisms are built into the following equation:

$$\frac{d}{ds} \left(\int_0^{b\sqrt{2}} [2\pi\rho ur] dr \right) = 2\pi b \rho_a \underbrace{\left[\alpha_1 |u^*(s)| + \alpha_2 u_a |\sin \theta| \cos \theta + \alpha_3 u' \right]}_{\alpha_4} \quad (25)$$

where α_1 is the entrainment coefficient of a free jet, α_2 is the entrainment coefficient for a line thermal, and α_3 is the entrainment coefficient caused by turbulence. The coefficients were assigned values of 0.057, 0.5 and 1.0 by Ooms. The variable u' represents the root mean squared turbulent fluctuation velocity in m/s because of atmospheric turbulence. A typical value suggested by Ooms

for u' is:

$$u' = (u_a^2)^{0.5} \quad (26)$$

where u_a is the wind velocity in m/s, and u' represents the root mean squared turbulent fluctuation velocity because of atmospheric turbulence in m/s. Briggs showed that:

$$u' = (\epsilon b)^{1/3} \quad (27)$$

where ϵ is the eddy energy dissipation. For a neutral atmosphere, Briggs assigns ϵ the value of $0.0677 \frac{u_a}{z}$, for $z < 300$ m. For unstable atmosphere, Kaimal [9] assigns ϵ the value 0.004 and a value of 0 for stable atmosphere.

When air is entrained into the jet, water moisture is also entrained into the jet depending on the atmospheric relative humidity. The molar rates of air and water moisture entrained into the jet depend on the atmospheric pressure, atmospheric temperature, and percent relative humidity:

$$\frac{d\dot{N}_a}{ds} = \frac{2\pi b \rho_a \alpha_4}{M_{w,a}} \quad (28)$$

$$\frac{d\dot{N}_{a,dry}}{ds} = \left(\frac{1}{1 + \alpha_w} \right) \frac{d\dot{N}_a}{ds} \quad (29)$$

$$\frac{d\dot{N}_w}{ds} = \left(\frac{\alpha_w}{1 + \alpha_w} \right) \frac{d\dot{N}_a}{ds} \quad (30)$$

$$\alpha_w = \frac{\frac{RH}{100} \times \frac{P_{w,sat}(T_a)}{P_a}}{1 - \frac{RH}{100} \times \frac{P_{w,sat}(T_a)}{P_a}} \quad (31)$$

where $\dot{N}_a$ is the rate of moist air entrainment into the jet in kmol/s, $\dot{N}_{a,dry}$ is the rate of dry air entrainment into the jet in kmol/s, $\dot{N}_w$ is the rate of water moisture entrainment into the jet in kmol/s, RH is the relative humidity in %, P_a is the absolute ambient atmospheric pressure in Pascal, $P_{w,sat}(T_a)$ is the water absolute vapor pressure in Pascal at the atmospheric ambient temperature T_a in K.

7.3 Conservation of Mass

The conservation of mass equation implies that no mass leaves the jet boundaries, i.e. is present outside the plume or jet boundaries:

$$\frac{d}{ds} \left(\int_0^{b\sqrt{2}} [2\pi cur] dr \right) = 0 \quad (32)$$

We note that the overall centerline mass fraction of the dispersing chemical or mixture, x , can be calculated from:

$$x = \left(\frac{c^*}{\rho^* + \rho_a} \right) = \left(\frac{c^*}{\rho} \right) = \left(\frac{c}{\rho} \right) \quad (33)$$

The centerline average molecular weight M_w and the overall centerline mole fraction of the dispersing chemical or mixture, y , can be calculated from:

$$M_w = \frac{\rho}{\left(\frac{c}{M_{w,o}}\right) + \left(\frac{\rho-c}{M_{w,a}}\right)} \quad (34)$$

$$y = \frac{\left(\frac{x}{M_{w,o}}\right)}{\left(\frac{x}{M_{w,o}}\right) + \left(\frac{1-x}{M_{w,a}}\right)} = \left(\frac{x}{M_{w,o}}\right) M_w = \left(\frac{c}{\rho}\right) \left(\frac{M_w}{M_{w,o}}\right) = \left(\frac{c^*}{\rho^* + \rho_a}\right) \left(\frac{M_w}{M_{w,o}}\right) \quad (35)$$

If the dispersing material is a mixture, then the individual species source mole or mass fractions can be multiplied by x or y to determine the jet individual species mole or mass fractions.

7.4 Conservation of Momentum

The equations of conservation of momentum yield the following two equations in the x and z directions, respectively:

X-Axis Momentum:

$$2\pi \frac{d}{ds} \left(\int_0^{b\sqrt{2}} [r \rho u^2 \cos \theta] dr \right) = 2\pi b \rho_a u_a \alpha_4 + C_d \pi b \rho_a u_a^2 |\sin^3 \theta| \quad (36)$$

Z-Axis Momentum:

$$2\pi \frac{d}{ds} \left(\int_0^{b\sqrt{2}} [r \rho u^2 \sin \theta] dr \right) = \int_0^{b\sqrt{2}} g(\rho_a - \rho) 2\pi r dr + \psi C_d \pi b \rho_a u_a^2 \sin^2 \theta \cos \theta \quad (37)$$

The first term on the right hand side of Equation 36 represents the increase in momentum caused by the inflow of air into the plume. The second term represents the increase in impulse caused by the drag force exerted by the wind on the plume. Ooms assign a value of 0.3 to C_d , the drag coefficient.

The variable ψ has a value of +1 if $-\pi/2 \leq \theta < 0$ and a value of -1 if $0 \leq \theta \leq \pi/2$. The first term on the right hand side of equation 37 represents the momentum change due to the difference in the density between the plume and the atmosphere. The second term represents the effects of the drag force in the z direction.

Benteboula et al. [8] present similar momentum equations with different entrainment and drag coefficients that account for the jet issuing into a cross-wind:

X-Axis Momentum:

$$2\pi \frac{d}{ds} \left(\int_0^\infty r \rho u^2 \cos \theta \cos \sigma dr \right) = u_a E + F_D \sqrt{1 - \cos^2 \theta \cos^2 \sigma} \quad (38)$$

Y-Axis Momentum:

$$2\pi \frac{d}{ds} \left(\int_0^\infty r \rho u^2 \cos \theta \sin \sigma dr \right) = -F_D \frac{\cos^2 \theta \sin \sigma \cos \sigma}{\sqrt{1 - \cos^2 \theta \cos^2 \sigma}} \quad (39)$$

Z-Axis Momentum:

$$2\pi \frac{d}{ds} \left(\int_0^\infty r \rho u^2 \sin \theta dr \right) = \pi \lambda^2 b^2 (\rho_a - \rho) g - F_D \frac{\sin \theta \cos \theta \cos \sigma}{\sqrt{1 - \cos^2 \theta \cos^2 \sigma}} \quad (40)$$

where F_D and E are defined as follows [8]:

$$F_D = C_D u_a^2 b \sqrt{2} (1 - \cos^2 \theta \cos^2 \sigma) \quad \text{with } 0.8 \leq C_D \leq 3 \quad (41)$$

$$E = 2\pi b \rho_a \left[\alpha + 0.5 u_a |\cos \theta \cos \sigma| \sqrt{1 - \cos \theta \cos \sigma} \right] \quad (42)$$

$$\alpha = \left(0.55 \sqrt{\frac{\rho^*}{\rho_a}} + 0.6 \frac{\sin \theta}{N_{Fr}} + 0.55 \frac{u_a \cos \theta \cos \sigma}{u_a + u^*} \right) u^* \quad (43)$$

$$N_{Fr} = \frac{u_j^2}{g d_j \left(1 - \frac{\rho_j}{\rho_a} \right)} \quad (44)$$

The momentum equations adjusted for cross-wind will add one more geometry variable to integrate, σ . [SuperChems® Expert](#) does not currently support calculations for jets issuing into a cross-wind. Only the X-axis and Z-axis momentum equations are currently used by [SuperChems® Expert](#) with the entrainment coefficients and parameters as originally proposed by Ooms.

7.5 Equation of State

Since the jet ultimately entrains a lot of air and the jet pressure is essentially at atmospheric pressure, the ideal equation of state is normally used to relate jet mass density (ρ in kg/m³) to jet temperature (T in K):

$$P = \frac{R_g T}{M_w} \rho = \text{constant} = \text{atmospheric pressure} \quad (45)$$

where R_g is universal gas constant = 8,314 J/kmol/K, and M_w is the average molecular weight of the jet dispersing material with the entrained air in kg/kmol. The average molecular weight can be calculated from the mass fractions of dispersing chemical, x_o and entrained air, $x_a = 1 - x_o$:

$$x_o = \frac{c}{\rho} \quad (46)$$

$$M_w = \frac{1}{\frac{x_o}{M_{w,o}} + \frac{1-x_o}{M_{w,a}}} = \frac{\rho}{\frac{c}{M_{w,o}} + \frac{\rho-c}{M_{w,a}}} \quad (47)$$

Using the above definition of mixture average molecular weight, the ideal equation of state can be rewritten as follows:

$$P = \frac{R_g T}{M_{w,a}} \left[c \left(\frac{M_{w,a}}{M_{w,o}} - 1 \right) + \rho \right] = \text{constant} = \text{atmospheric pressure} \quad (48)$$

Since P is constant, we can differentiate the above expression for pressure to relate centerline temperature to centerline concentration and density:

$$\frac{d}{ds} \left\{ (T_a + T^*) \left[c^* \left(\frac{M_{w,a}}{M_{w,o}} - 1 \right) + \rho_a + \rho^* \right] \right\} = 0 \quad (49)$$

Equation 49 can be used to solve for the centerline temperature when coupled with the conservation equations.

7.6 Conservation of Energy

The amount of heat emitted by the plume source per unit time is conserved. Assuming that the molecular weight and the specific heat of the pollutant being considered are not much different from those of air, Ooms presents the following energy conservation equation:

$$\frac{d}{ds} \left(\int_0^{b\sqrt{2}} \rho u \left(\frac{1}{\rho} - \frac{1}{\rho_{a,o}} \right) 2\pi r dr \right) = \rho_a 2\pi b \left(\frac{1}{\rho_a} - \frac{1}{\rho_{a,o}} \right) \alpha_4 \quad (50)$$

This equation assumes that the molecular weight and heat capacity of the gas are similar to that of air.

In a later publication, Ooms [10], rewriting his energy equation, assumed that vent gases had different molecular weights and heat capacities than those of air:

$$\frac{d}{ds} \left(\int_0^{b\sqrt{2}} \rho u c_p (T - T_{a,o}) 2\pi r dr \right) = \rho_a 2\pi b c_{p,a} (T_a - T_{a,o}) \alpha_4 \quad (51)$$

where $T_{a,o}$ is the ambient temperature in K at the jet axis elevation, c_p is the mixture specific heat capacity in J/kg/K, $c_{p,a}$ is the air specific heat capacity in J/kg/K, and:

$$T = \frac{M_w P}{R_g \rho} \quad (52)$$

$$T_a = \frac{M_{w,a} P}{R_g \rho_a} \quad (53)$$

$$M_w = M_{w,o} \frac{cT}{c_o T_o} + M_{w,a} \left(1 - \frac{cT}{c_o T_o} \right) \quad (54)$$

$$M_w c_p = M_{w,o} c_{p,o} \frac{cT}{c_o T_o} + M_{w,a} c_{p,a} \left(1 - \frac{cT}{c_o T_o} \right) \quad (55)$$

where $c_{p,o}$ is the dispersing chemical specific heat capacity in J/kg/K, T_o and c_o are the jet source temperature in K and concentration in kg/m³. These profiles are substituted into the energy equation to yield:

$$\frac{d}{ds} \left[\int_0^{b\sqrt{2}} \frac{M_w c_p}{M_{w,a} c_{p,a}} u \left[1 - \frac{\rho}{\rho_{a,o}} \left(1 + \frac{c\rho_o}{c_o \rho} \left(\frac{M_{w,a}}{M_{w,o}} - 1 \right) \right) \right] 2\pi r dr \right] = 2\pi b \left(1 - \frac{\rho_a}{\rho_{a,o}} \right) \alpha_4 \quad (56)$$

The term $M_w c_p / M_{w,a} c_{p,a}$ changes from an initial value of $M_{w,o} c_{p,o} / M_{w,a} c_{p,a}$ at the vent outlet to $\simeq 1$ in the far field. We follow Ooms in assuming an average value:

$$\frac{M_w c_p}{M_{w,a} c_{p,a}} = \frac{1}{2} \left(1 + \frac{M_{w,o} c_{p,o}}{M_{w,a} c_{p,a}} \right) \quad (57)$$

The jet centerline temperature, T , can be calculated from Equations 52 and 54:

$$T = \frac{1}{\frac{R_g(\rho^* + \rho_a)}{P_a M_{w,a}} + \frac{c}{c_o T_o} \left(1 - \frac{M_{w,o}}{M_{w,a}}\right)} \quad (58)$$

The energy equation can also be expressed differently using the similarity profile for temperature [8]. We can easily show that:

$$\frac{\rho c_p}{c_{p,a}} = c(\gamma - 1) + \rho \quad \text{since} \quad c_p = x_o c_{p,o} + (1 - x_o) c_{p,a} \quad (59)$$

$$\gamma = \frac{c_{p,o}}{c_{p,a}} \quad (60)$$

We can recast Equation 51 to the following form:

$$\frac{d}{ds} \left(\int_0^{b\sqrt{2}} [c(\gamma - 1) + \rho] u(T - T_{a,o}) 2\pi r dr \right) = \rho_a 2\pi b (T_a - T_{a,o}) \alpha_4 \quad (61)$$

The right hand side of Equation 61 can be modified to include heating and/or cooling by chemical reaction(s) (such as combustion) without the need for reaction rates assuming that the reaction(s) or combustion reaction(s) occur as fast as air is mixed into the jet and reaches chemical equilibrium quickly. [SuperChems[®] Expert](#) calculates such change in jet/plume temperature by calculating the chemical equilibrium conditions at each jet/plume curvilinear axial location. Multiphase chemical equilibrium in [SuperChems[®] Expert](#) is calculated using direct minimization of the Gibbs free energy.

7.7 Centerline Coordinates

The vertical (z) and horizontal (x) coordinates of the jet centerline can be calculated based on the value of the angle (θ) with respect to horizontal:

$$\frac{dz}{ds} = \sin \theta \quad (62)$$

$$\frac{dx}{ds} = \cos \theta \quad (63)$$

where s is the curvilinear distance of the jet centerline (see Figure 6) in m.

7.8 Air Density

We calculate the air density as a function of temperature, assuming that the temperature of the atmosphere decreases linearly with elevation:

$$T_a = T_{a,o} - Bz \quad (64)$$

where z is the elevation in meters and B is 0.0065 K/m. The air density is then calculated from:

$$P_a = P_{a,\circ} \left[1 - \frac{Bz}{T_{a,\circ}} \right]^{\frac{gM_{w,a}}{R_g B}} \quad (65)$$

$$\rho_a = \frac{P_a M_{w,a}}{R_g T} \quad (66)$$

The quantities $T_{a,\circ}$ and $P_{a,\circ}$ represent the ambient temperature and pressure at ground level or jet axis elevation.

7.9 Buoyancy Considerations for Flame Jets

Similar to Equation 61, recent work by Ekoto et al. [11] also modified the integral jet equations to enable buoyancy corrections and the independent solution of jet temperature to account for combustion heating for better modeling of flares and flame jets geometries and thermal radiation.

Ekoto's modification, however, does not use detailed chemical equilibrium calculations like SuperChems and updates the temperature by solving a simplified energy equation along with the equation of state.

SuperChems® Expert integrates chemical equilibrium with the differential equations and produces combustion/reaction species identities and profiles along the curvilinear jet/plume axis (see Figure 7). Such calculations are normally only possible by using detailed chemical kinetics models with computational fluid dynamics. **SuperChems® Expert** provides accurate reaction species and composition quickly (especially for combustion reactions) while computational fluid dynamics are resource intensive and cannot be practically used where large number of thermal radiation scenarios have to be considered in hazard and risk studies.

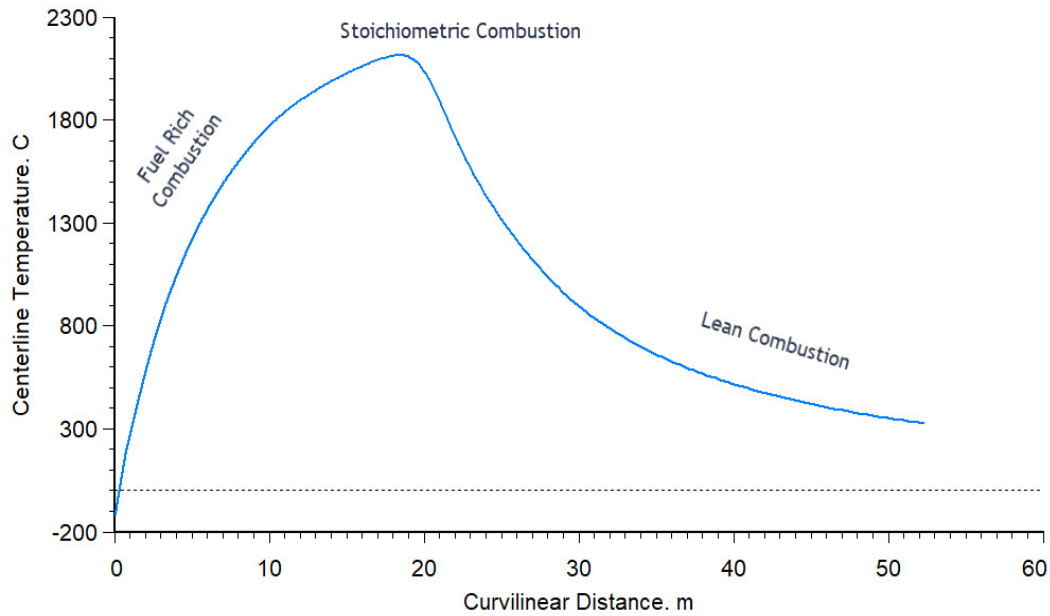
The assumption of chemical equilibrium is reasonable for combustion reactions. The integrated SuperChems detailed chemical equilibrium calculations assume that chemical equilibrium is reached as soon as the air is entrained and mixed into the jet. Because SuperChems uses direct minimization of the Gibbs free energy for chemical equilibrium, only the starting composition of the fuel is needed. As air and water moisture are entrained into the jet, the potential combustion products are automatically selected and the flame temperature is calculated. This equilibrium method can be easily extended to multiphase jets.

Figure 8 illustrates the impact of combustion reactions consideration on jet density. For this particular simulation of the large scale test (see Section 10.2), the jet density reaches that of air as more air is entrained into the jet and neutral buoyancy is achieved. However, with combustion heating the jet gains buoyancy and remains buoyant. The buoyancy drastically alters the geometry of the jet as shown in Figure 13.

7.10 Differential Balances and Equations

The similarity profiles described by Equations 20, 21, 22 and 23 are substituted in the conservation equations. The integrals are evaluated analytically to produce a set of ordinary differential equa-

Figure 7: Calculated flame jet centerline temperature using chemical equilibrium for large scale hydrogen Test 2 (see Section 10.2)



Source: [SuperChems® Expert](#)

tions that can be integrated for $\frac{d\rho^*}{ds}$, $\frac{du^*}{ds}$, $\frac{dc^*}{ds}$, $\frac{dT^*}{ds}$, $\frac{db}{ds}$, $\frac{d\theta}{ds}$, $\frac{dz}{ds}$, and $\frac{dx}{ds}$. The differential equations are integrated numerically using [SuperChems® Expert](#) with combustion and flame temperatures calculated at every integration step using direct minimization of the Gibbs free energy.

7.11 Modifications to Ooms Model

The similarity profiles proposed by Ooms were valid only for the region of established flow. In the zone of flow establishment, the jet initially exits with a “top hat” profile for concentration and velocity. As air is entrained because of turbulence, the profile is transformed into a Gaussian profile. Thus the x and z coordinates at which the plume profile is Gaussian need to be calculated.

Kamotani and Greber [12] developed a correlation for the calculation of the zone-of-flow establishment height for a turbulent jet in a cross wind:

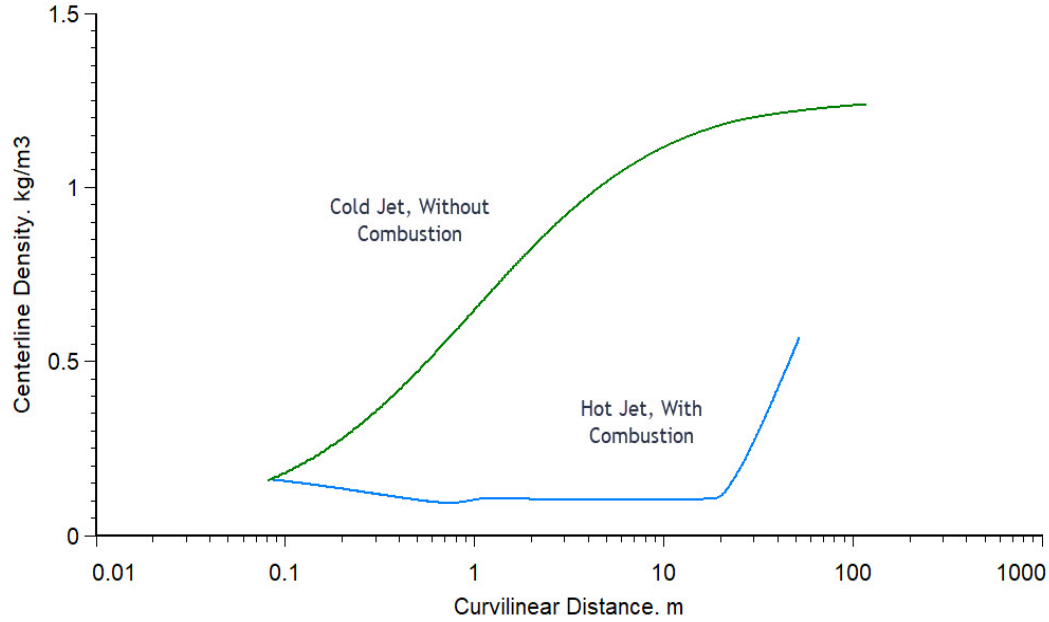
$$\frac{\delta}{D} = a \left(\frac{X}{D} \right)^b \quad (67)$$

where, a and b are empirical dimensionless parameters calculated from experimental data. These parameters are calculated as a function of the jet momentum ratio, J :

$$J = \left(\frac{\rho_j}{\rho_a} \right) \left(\frac{u_j}{u_a} \right)^2 \quad (68)$$

Reference [12] showed graphs for the calculation of a and b . These graphs were regressed and incorporated into [SuperChems® Expert](#) software. A simplified equation was reported by Kamotani

Figure 8: Calculated flame jet centerline density using chemical equilibrium for large scale hydrogen Test 2 (see Section 10.2)



Source: [SuperChems® Expert](#)

and Greber:

$$\frac{\delta}{D} = 0.89J^{0.89} \left(\frac{X}{D} \right)^{0.36} \quad 15 \leq J \leq 60 \quad 0 \leq \frac{X}{D} \leq 20 \quad (69)$$

The plume path length necessary to develop a Gaussian profile may be calculated using the data of Keefer and Baines [13]. They investigated axial velocity decay for jets of air normal to a flowing stream. They reported values for the start of axial velocity decay for jets with velocity ratios u_j/u_a of 4, 6, and 8 for values of $\frac{s}{D}$ of 1.59, 1.92, and 2.3, respectively. These data are fit into the equation:

$$\frac{s}{D} = 0.871667 + 0.1775 \frac{u_j}{u_a} \quad (70)$$

The equations of Kamotani and Keefer are solved simultaneously to produce the initial starting coordinates for Ooms' model. The procedure is as follows:

1. Calculate the average molecular weight of the jet:

$$M_{w,j} = \frac{p^*}{760} M_{w,c} + \left(1 - \frac{p^*}{760} \right) M_{w,a} \quad (71)$$

where p^* is the pressure of the chemical being released and $M_{w,c}$ is the molecular weight of the chemical being released.

2. Calculate the jet momentum ratio:

$$J = \left(\frac{M_{w,j}}{M_{w,a}} \right) \left(\frac{u_j}{u_a} \right)^2 \quad (72)$$

3. Calculate a , b , and the necessary path length $\frac{s}{D}$.

4. Solve the following equation for θ :

$$\frac{s}{D} \sin \theta - a \left(\frac{s}{D} \cos \theta \right)^b = 0 \quad (73)$$

5. Calculate the initial values of x , z and s from:

$$s = \left(\frac{s}{D} \right) D \quad (74)$$

$$x = s \cos \theta \quad (75)$$

$$z = s \sin \theta \quad (76)$$

Initial values for the concentration, density, and plume radius must also be calculated.

7.12 Initial Values for Ooms' Model

The mass flow rate from the expanded conditions of the flow source is calculated from:

$$\dot{m} = \frac{\pi D^2}{4} \rho u_j \quad (77)$$

This is, by Ooms' definition, equal to:

$$\dot{m} = \int_0^\infty c^* e^{-r^2/\lambda^2 b^2} \left(u_a \cos \theta + u^* e^{-r^2/b^2} \right) 2\pi r dr \quad (78)$$

Integrating this equation and equating it to the previous one, it can be shown that the initial value of the plume radius must be:

$$b = \frac{D \sqrt{1 + \lambda^2}}{2\lambda \sqrt{\frac{u_a \cos \theta}{u_j} \lambda^2 + 1}} \quad (79)$$

The initial values for the remaining variables are:

$$c^* = \frac{4\dot{m}}{\pi D^2 u_j} \quad (80)$$

$$u^* = u_j - u_a \cos \theta \quad (81)$$

$$\rho^* = c^* - \rho_a \quad (82)$$

Ooms' model, without combustion, has been validated by many researchers (See Havens [14], Astleford [15], Pfenning [16]). The data used in testing consisted of small scale wind tunnel data, and real experimental data on propane and ammonia. Good agreement was reported by all sources. It is important to note however, that the jet model produces a circular cross-section of the plume. This is not realistic since the entrainment in the lateral direction is greater than that in the vertical direction in the far field. This produces an elliptic cross-section of the plume. An elliptic shape is consistent with the Gaussian (Pasquill- Gifford) dispersion coefficient representation of atmospheric turbulence entrainment.

7.13 Using Ooms Model

Ooms model is best used for limiting concentrations greater than or equal to a few thousand ppm. In general, Ooms model is not recommended for toxicity assessment at low concentrations, say less than 1000 ppm. It is an accurate model to use for flammable dispersion analysis and to determine the amount of material within the flammable range for a free unobstructed jet.

It is best to transit from the Ooms model to a Gaussian model (vertical plane or puff) at the point where the jet momentum and buoyancy effects become small and wind speed dominates the dispersion. It is also recommended to transit to a vertical plane Gaussian model when the jet hits an obstruction or the ground. This is much more preferred than a transition to a Gaussian point or line source model since a vertical plane model preserves more of the jet/cloud physical dimensions at the transition point. A transition to a Gaussian puff model is recommended for short duration releases.

8 Solid Flame Jet Models

The flame jet can also be represented using a solid flame geometry. Kalghatgi [17, 18] conducted an experimental study which investigated the shape of hydrocarbon turbulent diffusion flames issuing into a cross-wind with a release angle θ_j relative to the horizontal wind direction as shown in Figure 9. He fit the results of his study to curves which characterize various features of the jet such as the non-dimensional rise, z , of the flame tip above the nozzle, and the angle, θ , the burner axis makes with the line joining the burner port and the flame tip.

Besides locating the tip of the flame jet, Kalghatgi [17, 18] used his results to derive empirical relationships which approximate the flame's shape with the frustum of a cone as illustrated in Figure 9. In the dimensionless coordinates defined by Equations 107 and 108, Kalghatgi [17, 18] predicts that the tip of a hydrocarbon flame jet issuing into a cross-wind is given by:

$$x = z \cot(90 - \theta) \quad (83)$$

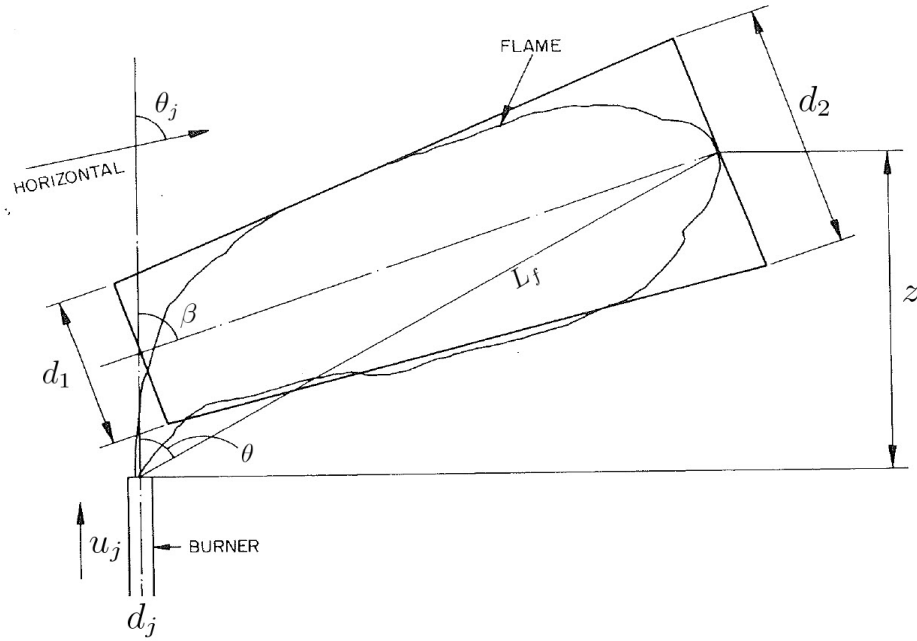
where

$$z = \left[20 \left(\frac{u_a}{u_j} \right) + 2.35 \left(\frac{u_a}{u_j} \right)^{-1} + 6 \right] d_j \sqrt{\frac{\rho_j}{\rho_a}} \quad (84)$$

$$\theta = 94 - 1.6 \left(\frac{u_a}{u_j} \right)^{-1} - 35 \left(\frac{u_a}{u_j} \right) \text{ (in degrees)} \quad (85)$$

$$\beta = 94 - 1.1 \left(\frac{u_a}{u_j} \right)^{-1} - 30 \left(\frac{u_a}{u_j} \right) \text{ (in degrees)} \quad (86)$$

Figure 9: Kalghatgi solid flame model



Source: [17] and ioMosaic®

$$L_f = \frac{z}{\sin(90 - \theta)} \quad (87)$$

$$d_1 = \left[49 - 0.22 \left(\frac{u_a}{u_j} \right)^{-1} - 380 \left(\frac{u_a}{u_j} \right) + 950 \left(\frac{u_a}{u_j} \right)^2 \right] d_j \sqrt{\frac{\rho_j}{\rho_a}} \quad (88)$$

$$d_2 = \left[80 - 0.57 \left(\frac{u_a}{u_j} \right)^{-1} - 570 \left(\frac{u_a}{u_j} \right) + 1470 \left(\frac{u_a}{u_j} \right)^2 \right] d_j \sqrt{\frac{\rho_j}{\rho_a}} \quad (89)$$

The geometric view factor for this particular flame shape can be estimated by recasting the cone into a tilted cylinder of an equivalent area. The equivalent cylinder diameter and length are calculated as follows:

$$L_f = \frac{z \sin \theta}{\sin(90 - \theta) \sin(180 - \beta)} \quad (90)$$

$$D_f = 2 \sqrt{\frac{d_1 + d_2}{L_f}} [L_f^2 + d_2 - d_1]^{1/4} \quad (91)$$

where L_f and D_f are the length and diameter of the cylindrical flame surface equivalent to the cone representation of the flame as shown in Figure 9.

9 Other Flame Geometry Models

Additional simplified methods for calculating of flame geometry are discussed below. These methods are typically focused on the estimation of flame jet length.

9.1 MIT Correlation

The landmark paper on the subject of turbulent jet diffusion flames came from Hawthorne et al. [19] in which a correlation predicting the length of a turbulent jet is derived based on conservation of mass and momentum arguments, the perfect gas law and an empirical relationship between the length and spread of the jet. The length correlation which is obtained from the author's analysis relies on the following assumptions:

- the temperature, concentration and velocity profiles are uniform over the cross-section of the jet,
- the spread angle of the jet is constant along the length of the jet,
- no pressure gradient exists along the length of the jet,
- the flow is steady and turbulent,
- the reaction rate is infinitely fast,
- the force of buoyancy is negligible, and
- the surrounding fluid is stagnant.

They further postulated that the length of a free flame jet is proportional to the flame spread at the axial distance where the nozzle gas concentration is the stoichiometric value required for complete combustion. In keeping with the aforementioned assumptions, the free visible flame jet length expressed as a function of stoichiometric concentration is given by:

$$\frac{L_f}{d_j} = \frac{530}{C_T} \sqrt{\frac{T_f}{\alpha_T T_j} \left[\frac{C_T}{100} + \left(1 - \frac{C_T}{100}\right) \frac{M_{w_a}}{M_{w_j}} \right]} \quad (92)$$

where L_f is the visible flame length, d_j is the nozzle diameter, T_f is the absolute adiabatic flame temperature, T_j is the absolute temperature of the nozzle fluid, M_{w_a} is the molecular weight of air, M_{w_j} is the molecular weight of the nozzle fluid, C_T is the mole % of the nozzle fluid at stoichiometric conditions or complete combustion, and α_T is the molar ratio of reactants to products at stoichiometric conditions.

This result must be treated with skepticism for several reasons. The main concerns of the authors regarded the limited range of port diameters which were used to estimate the correlation between the length and spread of the jet, in addition to the possibility that at higher Froude numbers the flame lengths would be greater than the correlation predicts. Brzustowski [20] noticed that the entrainment in most of the flames studied by Hawthorne et. al. [19] was substantially influenced by the force of buoyancy, although Equation 92 was derived under the assumption of negligible buoyancy.

If we apply Equation 92 to methane flow at 298 K, where $C_T = 9.5\%$, $\alpha_T = 1$, and $T_f = 2,238$ K, we calculate a flame length to nozzle diameter ratio of $\frac{L_f}{d_j} \simeq 202$. A 4 inch methane release at those conditions would yield a flame jet length of 808 inches or 67 ft. The author [19] reports a maximum error range between 10 and 20 %.

Brzustowski [20] points out that Equation 92 can be simplified for hydrocarbons without too much loss in accuracy based on typical values of C_T and α_T to yield:

$$\frac{L_f}{d_j} \simeq \frac{1500}{C_T} \sqrt{\frac{M_{w_a}}{M_{w_j}}} \quad (93)$$

If we apply Equation 93 to the same methane flow, we calculate a flame length to nozzle diameter ratio of $\frac{L_f}{d_j} \simeq 213$.

9.2 Lean-Limit Hypothesis

Brzustowski [20] conceived of and tested a hypothesis for predicting flame jet lengths based on the lean-limit concentration. Brzustowski's hypothesis stated that: "The end of a turbulent diffusion flame at a very high Reynolds number occurs at that point on the axis of maximum fuel concentrations where the fuel concentration equals the lean limit". This is equivalently expressed as:

$$\frac{L_f}{d_j} = \frac{x}{d_j} \Big|_{C_m = C_{LFL}} \quad (94)$$

where C_m is the % by volume of fuel concentration on the axis of maximum concentrations, and C_{LFL} is the % by volume of fuel concentration at the lower flammability limit (LFL).

Brzustowski [20] used this hypothesis to derive the following expressions for predicting the flame jet length for both momentum and buoyancy dominated jets. For momentum dominated jets, he obtained:

$$\frac{L_f}{d_j} = \frac{\mathcal{Y}_j}{0.32} \left(\frac{\rho_j}{\rho_a} \right)^{1/2} \left[1 + \frac{M_{w_a}}{M_{w_j}} \left(\frac{100}{0.297 C_{LFL}} - 1 \right) \right] \quad (95)$$

where C_{LFL} is the fuel lower flammable limit in volume or mole %, and $\mathcal{Y}_j$ is the mass fraction of the fuel. For jets dominated by the force of buoyancy, he obtained:

$$\frac{L_f}{d_j} = 2.96 \left(\frac{\rho_j}{\rho_a} \right)^{1/2} N_{Fr}^{1/5} \mathcal{Y}_j^{3/5} \left[1 + \frac{M_{w_a}}{M_{w_j}} \left(\frac{100}{0.297 C_{LFL}} - 1 \right) \right]^{3/5} \quad (96)$$

where N_{Fr} is the Froude number with combustion:

$$N_{Fr} = \frac{u_j^2}{g d_j} \left(\frac{\rho_a}{\rho_j} \right)^{1/2} \frac{c_{p,a} T_a}{\mathcal{Y}_j \Delta H_c + c_{p,j} (T_j - T_a)} \quad (97)$$

where ΔH_c is the heat of combustion per unit mass, g is the gravitational constant or acceleration due to gravity, $c_{p,a}$ is the air specific heat, and $c_{p,j}$ is the fuel specific heat.

The above correlations were formulated under the assumptions that the concentration profiles were

of the cosine form described by Field et. al. [21], and the entrainment occurs in the manner according to Putnam and Speich [22].

9.3 Froude Number Correlations

Putnam and Speich [22] correlated data which they had compiled on buoyancy dominated gaseous diffusion jets to obtain flame lengths for circular ducts given by:

$$\frac{L_f}{d_j} = 26 N_{Fr}^{1/5} \quad (98)$$

where N_{Fr} is the Froude number defined as the dimensionless quantity $u_j^2/(gd_j)$. Equation 98 is assumed to have been correlated for city gas similar to methane and fits the data for Froude numbers up to 3×10^4 . For Froude numbers greater than 3×10^4 , no dependence of the flame length on Froude number appears to exist.

Similar correlations developed by Suris et. al. [23] and Sonju and Hustad [24] were obtained which related the flame jet length to the Froude number according to a generalization of Equation 98 given by:

$$\frac{L_f}{d_j} = A N_{Fr}^{1/5} \quad (99)$$

where Suris et. al. [23] reported the coefficient A as being 29 for methane and 40 for propane while Sonju and Hustad [24] reported it as being 21 for methane and 27 for propane. Equation 99 was reported by Suris et. al. [23] as being valid for Froude numbers less than 3×10^4 , with no dependence on Froude number beyond that value, while Sonju and Hustad [24] found the correlation to extend to Froude numbers up to 10^5 .

Delichatsios [25] proposes the following correction for flame jet length and width:

$$L_f = \frac{L^*}{Y_s} d^* \quad (100)$$

$$W_f \simeq 0.17 L_f \quad (101)$$

$$L^* = \frac{13.5 N_{Fr}^{0.4}}{(1 + 0.07 N_{Fr}^2)^{0.2}} \text{ for } N_{Fr} < 5 \quad (102)$$

$$= 23 \text{ for } N_{Fr} \geq 5 \quad (103)$$

$$N_{Fr} = \frac{u_j Y_s^{1.5}}{\left(\frac{\rho_j}{\rho_a}\right)^{0.25} \left(\frac{T_f - T_a}{T_a} g d_j\right)^{0.5}} \quad (104)$$

$$d^* = d_j \sqrt{\frac{\rho_j}{\rho_a}} \quad (105)$$

where Y_s is the stoichiometric fuel mass fraction, d_j is the expanded jet flow diameter, ρ_j is the expanded jet density, u_j is the expanded jet velocity, and T_f is the adiabatic flame temperature.

9.4 Flame Axis Correlations

Pratt and Baines [26] proposed a cold flow correlation which defines in rectangular coordinates, the center line axis of a non-reacting turbulent jet issuing into a cross-wind. Hoehne and Luce [27] generalized their result to include the effect of buoyancy resulting from jets having densities other than that of air. This generalized result is given by:

$$Z = 2.05 X^{0.28} \quad (106)$$

where the non-dimensional coordinates X and Z are functions of the flow conditions and densities of the nozzle and ambient fluids, and are defined by:

$$X = \frac{x}{d_j} \left(\frac{\rho_a}{\rho_j} \right)^{1/2} \left(\frac{u_a}{u_j} \right) \quad (107)$$

$$Z = \frac{z}{d_j} \left(\frac{\rho_a}{\rho_j} \right)^{1/2} \left(\frac{u_a}{u_j} \right) \quad (108)$$

While Equation 106 well describes the axis of the jet downstream of the nozzle, it fails to adequately predict the shape of the axis near the nozzle. Pratt and Baines [26] describe this region as a potential core since the effects of mixing are not felt here due to the low turbulence intensity associated with this region. Thus the motion of the jet in the potential core may be characterized by potential flow theory. Brzustowski [28] derived an asymptotic near-nozzle solution for this region which resulted in the expression for the center-line axis given by:

$$Z = 2.5 X^{1/2} \quad (109)$$

9.5 Lean-Limit Method

Brzustowski [29] proposed a method for locating the tip of a flame jet issuing into a cross-wind based on the reduced lean-limit concentration of the fuel, $\bar{C}_L$ given by:

$$\bar{C}_{LFL} = \frac{C_{LFL}}{100} \left(\frac{M_{w_j}}{M_{w_a}} \right) \left(\frac{u_j}{u_a} \right) \quad (110)$$

where C_{LFL} is the lean flammability limit concentration in mole or volume %. The downstream distance x_{LFL} corresponding to the lean-limit concentration in the jet, is related to the dimensionless distance s_{LFL} , as measured along the axis of maximum gas concentration according to:

$$x_{LFL} = s_{LFL} - 1.65 \quad (\text{for } \bar{C}_{LFL} \leq 0.5, \text{ or } s_{LFL} > 2.35) \quad (111)$$

If $\bar{C}_{LFL} > 0.5$, and $s_{LFL} \leq 2.35$, x_{LFL} is given by the solution of the algebraic equation:

$$1.04 x_{LFL}^2 + 2.05 x_{LFL}^{0.28} = s_{LFL} \quad (112)$$

where the axial distance s_{LFL} is defined by:

$$s_{LFL} = \frac{s}{d_j} \left(\frac{\rho_a}{\rho_j} \right)^{1/2} \left(\frac{u_a}{u_j} \right) \quad (113)$$

where s is the axial distance from the nozzle. s_{LFL} is determined from the lean flammability limit concentration according to:

$$s_{\text{LFL}} = \begin{cases} 2.04 \bar{C}_{\text{LFL}}^{-1.03} & \text{for } (\bar{C}_{\text{LFL}} \leq 0.5) \\ 2.51 \bar{C}_{\text{LFL}}^{-0.625} & \text{for } (\bar{C}_{\text{LFL}} > 0.5) \end{cases} \quad (114)$$

where $\bar{C}_{\text{LFL}}$ is determined from Equation 110. Once the dimensionless downstream distance, x_{LFL} is determined from Equations 111 and 112, the dimensionless rise, Z_{LFL} of the flame tip above the nozzle is calculated from the aforementioned correlation proposed by Pratt and Baines [26] given by Equation 106. Finally, the dimensional coordinates, x and z , may be ascertained from the non-dimensional coordinate values of the flame tip using Equations 107 and 108.

The lean-limit method for predicting the tip of a jet issuing into a cross-wind should not be used for values of $u_j/u_a > 110$. If the behavior in stagnant air is to be determined, one should not use an infinite value for the ratio of nozzle velocity to wind speed, but rather the maximum value of 110 seems to work best [29]. This method for predicting the length of a hydrocarbon flame jet issuing into stagnant air is recommended over using the MIT correlation given by Equation 92 [29].

It should be noted that the lean-limit method only characterizes the shape and length of the central axis of the jet. It makes no attempt at predicting the maximum extent of jet spread nor does it attempt to characterize the rate of jet spread with respect to the jet's axis.

10 Selected **SuperChems® Expert** Validation

SuperChems® Expert has been validated against numerous incidents, small scale tests, and large scale tests. We highlight three high quality large scale data sets where the visible flame length and incident thermal radiation flux were measured for methane and hydrogen.

10.1 Case 1: John Zink Flare Data

In the July 1997 issue of Chemical Engineering Progress (CEP), Schwartz and White of John Zink Company published their work comparing flare thermal radiation test data to several thermal radiation calculation and prediction methods [30].

In addition to discussing general radiation fundamentals and providing a practical approach to predict thermal radiation, Schwartz and White [30] provided two examples comparing several available radiation prediction methods/models (Oenbring & Sifferman (O&S), Tan, API-521-S, API-931, Kent, API-521-B&S, and GPSA) with actual flare thermal radiation test data.

The **SuperChems® Expert** thermal radiation levels predicted with the multiple point source flame jet model are in excellent agreement with the measured data. Case study 1 was selected for validation. This case study is based on an actual flare system located in a gas producing facility. The flare stack is equipped with a flare burner that has pilots and flame stabilization devices. The process and physical data required for calculation of thermal radiation using **SuperChems® Expert** and other methods are listed in Table 3.

Figure 10 compares the calculated data from **SuperChems® Expert** and other methods with the

Table 3: Flare test conditions and dimensions

Variable	Value
Flow Rate	84,360 kg/hr
Gas composition	
CH ₄	85.0 vol %
H ₂ S	0.1 vol %
CO ₂	7.0 vol %
N ₂	7.9 vol %
Gas Molecular Weight	18.96 kg/kmol
Gas Temperature	50.0 °C
Lower Heating Value	8,600 kcal/kg
Outlet Diameter	0.610 m
Stack Height	46 m
Wind Velocity at Stack Height	3.7 m/s
Relative humidity (assumed)	70 %
Atmospheric Stability class (assumed)	D (neutral)

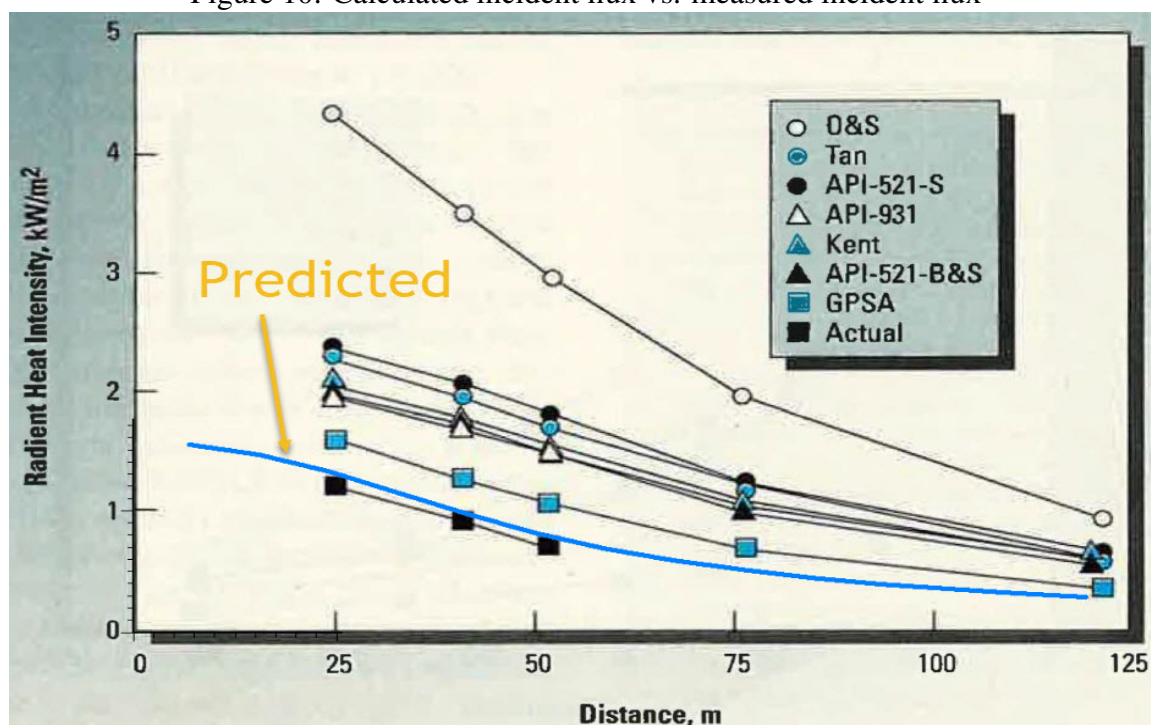
Table 4: Fraction Radiated Calculated by Method

Method	Fraction Radiated
Kent	0.185
Tan	0.209
API-521-S	0.150
API-931	0.170
O&S	0.250
API-521-B&S	0.150
GPSA	0.100
SuperChems® Expert	0.151

actual measured data. Here the radiant heat flux calculated using **SuperChems® Expert** and each complete method is plotted against the distance from the base of the flare stack. As can be seen from Figure 10, **SuperChems® Expert** predictions are closest to the actual test data. **SuperChems® Expert** also predicted a radiation fraction of 15 %. The values used for the other methods are shown in Table 4.

Figure 11 shows the flare/jet path and flame path centerline calculated by **SuperChems® Expert**. The indicated symbols are the radiant epicenter locations predicted by the corresponding methods relative to the flame observed during the test of the flare. **SuperChems® Expert** generates a reasonable flame path, which has its path length of 46.5 meters. The buoyancy impact on flare flame geometry is very important. If flame buoyancy is ignored and a cold flare/jet path and shape were calculated, the flame length would have been longer and the flame geometry narrower. Although the flame geometry does not have a large impact on the incident flux in the far field, it does have an impact on the near field incident flux estimates.

Figure 10: Calculated incident flux vs. measured incident flux



Source: ioMosaic® and [30]

10.2 Case 2: Large Scale Hydrogen Flame Jet Test Data

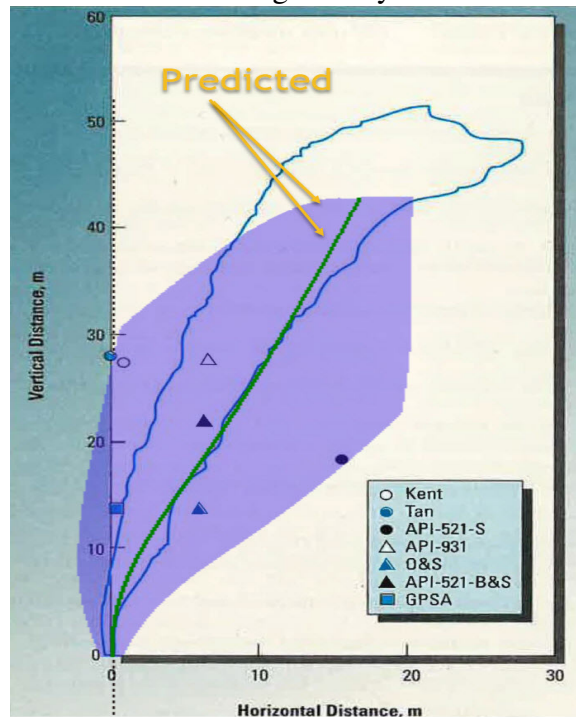
Two large scale hydrogen flame jet experiments were conducted at the GL Noble Denton Spadeadam Test Site in North Cumbria, United Kingdom (UK) [11, 31]. Compressed hydrogen gas was released from a nominal 60 barg stagnation pressure through a horizontal 1 meter long pipe 3.25 m above the ground. Two different internal diameters of 20.9 and 52.5 mm were used. A 25 m x 15 m concrete pad below the release point was used to prevent surface dirt entrainment into the flame. To protect against spalling, the pad was further covered with steel sheeting. The test data is summarized in Table 5.

To properly model these two large scale tests in **SuperChems® Expert**, the choked flow conditions at the end of the release pipe have to be calculated first, and then these have to be expanded to atmospheric conditions to establish the flame jet starting conditions. The end of pipe and expanded conditions are shown in Table 6.

Because the flow is horizontal and because the fuel is hydrogen, the impact of buoyancy on flame shape is significant. If buoyancy is ignored and a cold jet is calculated to obtain the flame jet path and geometry, the flame jet dominant buoyancy observed towards the end of jet would not be calculated. Excellent flame jet length and geometry predictions are provided by **SuperChems® Expert**, 17.5 m for Test 1, and 52.2 m for Test 2.

The flame jet geometries and length are shown in Figures 12 and 13. Excellent agreement is obtained, despite the flame jets impacting the ground, and despite the use of entrainment parameters as originally proposed by Ooms without any adjustments or fine tuning.

Figure 11: Calculated flare flame geometry vs. observed flare geometry



Source: *ioMosaic*[®] and [30]

To illustrate the impact of combustion on buoyancy, we compare the calculated flame jet path for Test 2 with and without the combustion contribution. As shown in Figure 14, the flame jet lifts off earlier and is shorter due to the contribution of combustion induced buoyancy. The flame jet length is overestimated for the cold flame jet case (limiting concentration of 4 % for hydrogen). The geometry of the flame jet is important for the estimation of incident flux in the near field which depends on how much of the flame surface is visible to a particular receptor of interest.

10.3 Case 3: Large-Scale ADL Gas Flame Jet Experiments

This experiment was conducted by Arthur D. Little Inc. (ADL) in 1989 to obtain experimental verification for flame jet length and safe separation distance from high pressure and large diameter gas releases.

The trials took place on 22 May 1989 in the Middle East. Following a workover a Gas well (see Figure 3) was flared for one hour from a permanent horizontal flare under the following conditions:

- Ambient Temperature of 35 degrees C
- Relative Humidity of 35 %
- Sunny, cloudless sky
- Steady wind at 90 degrees to direction of flare ranging from 6 to 8 m/s

Design details of well and flare line are summarized as follows:

Table 5: Large scale hydrogen flame jet test data

Test Condition	Test 1	Test 2
Flow diameter, mm	20.9	52.5
Flow rate, kg/s	1.0	7.4
Source pressure, bara	60.822	63.11
Source temperature, K	308.7	287.8
Ambient pressure, bara	1.022	1.011
Ambient temperature, K	280	280
Relative humidity, %	94.3	94.5
Wind speed, m/s	2.84	0.83
Wind angle, degrees	68.5	34.0
Visible flame length, m	17.4 +/- 1.1	45.9 +/- 2.5

Table 6: Calculated large scale hydrogen flame jet test data end of pipe conditions

Source Parameters	Test 1	Test 2
Choke pressure, bara	27.72	29.7
Choke temperature, K	269.44	251.75
Expanded diameter, mm	65.64	170.91
Expanded pressure, bara	1.02	1.01
Expanded temperature, K	166.73	155.07

- Flare line is 650 m long (measured from wellhead)
- The flare line has a 4.5 inch outside diameter with 0.531 inch wall thickness (Schedule 160) giving an internal flow diameter of 3.44 inch.
- Well depth to gas reservoir is 10,000 ft
- The well reservoir pressure is 4,600 psig
- A flow meter was installed close to end of flare line
- A pressure gauge was installed at wellhead to measure flowing pressure during flaring

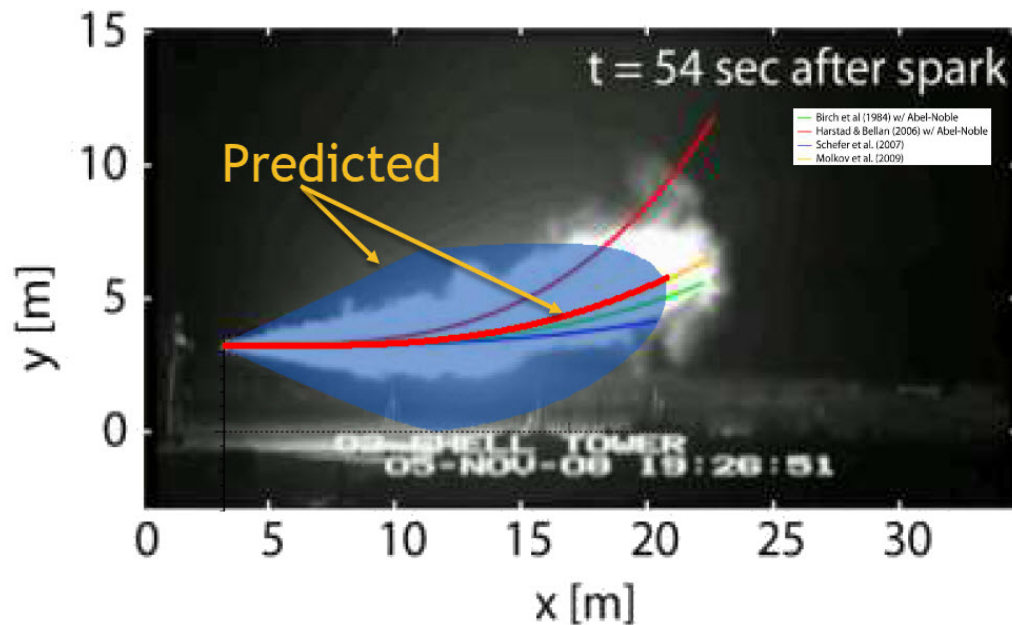
Photographs were taken at 90 deg from the flare at a distance of about 150 to 200 meters and fixed points were measured to enable the flame jet lengths to be estimated by comparison. This is illustrated in Figure 3. Close up photos were also taken at about 75 to 100 meters.

The data is summarized in Table 7 covers a range of flow rates increasing from 20 MMSCFD to a maximum of 80 MMSCFD. At the 80 MMSCFD the flowing wellhead pressure was 3000 psig.

Table 7: Full-scale flame jet data summary

Wellhead Pressure (psig)	Flow Rate (MMSCFD)	Stand Off Distance (m)	Flame Jet Length (m)	≈ Distance to 1 kW/m ² (m)
2,000	20	2.5 to 3.0	22 to 26	75
3,000	80	3	33 to 37	100

Figure 12: Calculated flame jet geometry vs. observed flame jet geometry for hydrogen Test 1



Source: ioMosaic® and [11]

SuperChems® Expert simulation results for 3,000 psig case are shown in Figure 16. The predictions for flame jet length and thermal radiation distance are in excellent agreement with the large scale field observations, despite the flame jet impingement on the ground surface. The calculated distance to 1 kW/m^2 is 112 m, which is 12 % more than the approximate observed distance of 100 m.

11 SuperChems Flame Jet/Flare Model

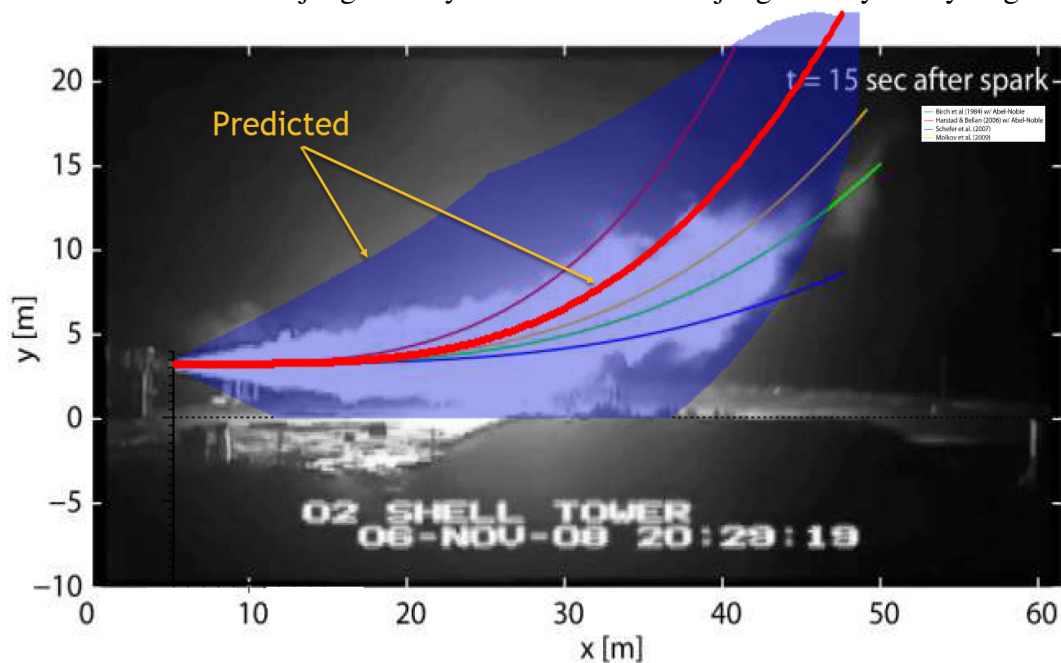
SuperChems numerically integrates the 2D similarity profiles discussed earlier along with detailed chemical equilibrium (by direct minimization of the Gibbs free energy) to establish the combustion reaction flame temperature and species compositions as a function of the curvilinear jet/flare axis.

Unlike models that use cold jet dispersion equations to establish the flame length, orientation, and geometry, SuperChems® Expert provides a much better representation of both momentum and the combustion buoyancy impact on flame jet/flare geometry as shown from measured large scale test data for natural gas and hydrogen flames (see Figures 11, 12, 13, and 16).

11.1 Multiple Burner Configurations

Multiple burner configurations can be modeled in SuperChems® Expert to develop noise and thermal radiation profiles (horizontal and vertical isopleths) at specific limiting thresholds or user specified locations. Thermal radiation probits and/or criteria can also be used to develop such profiles

Figure 13: Calculated flame jet geometry vs. observed flame jet geometry for hydrogen Test 2



Source: ioMosaic® and [11]

for injury, fatality, or equipment damage. In addition, contributions from separate multiple facility flares at different locations can be added and displayed on site maps for both thermal radiation and noise.

11.1.1 Thermal Radiation

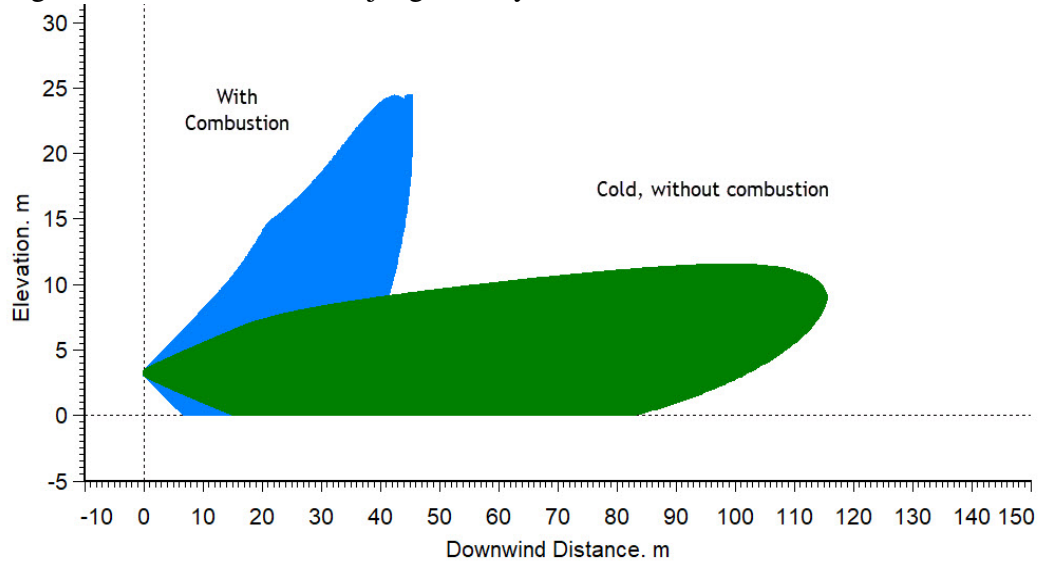
Multiple burner configurations yield shorter jets with better mixing and cleaner combustion. As a result, thermal radiation in the near field is reduced, although far field values are not significantly impacted since the flame will resemble a point source far away from the flare. In addition, shorter high momentum flames are highly stable and resist wind deflection. Better mixing into the flame jet core can yield less soot and less thermal radiation.

11.1.2 Noise

Sound power level (noise) can also be reduced by using multiple burner configurations. Multiple burner configurations, lower the required exit velocity and pressure differential per burner tip (acoustic efficiency), drastically cutting down high-frequency jet noise.

SuperChems calculates flame jet/flare noise from the flow mechanical energy and combustion with options for sound power level weighting and atmospheric noise attenuation. SuperChems calculates the flow sound power level in a fundamental way [32] by assigning an acoustic efficiency

Figure 14: Calculated flame jet geometry for Test 2, with and without combustion



Source: SuperChems[®] Expert

factor to provide the fraction of flow mechanical energy that is converted to noise:

$$W = \frac{\eta}{2} \dot{m} u^2 \quad (115)$$

$$L_{W,T} = 10 \log_{10} \left[\frac{W}{10^{-12}} \right] = 10 \log_{10} W + 120 = 10 \log_{10} \left[\frac{\eta}{2} \dot{m} u^2 \right] + 120 \quad (116)$$

where W is the flow mechanical power or energy, L_W is the sound power level³ in dB, and η is defined as the acoustic efficiency factor. Equation 116 shows that one Watt of acoustic energy is equivalent to a sound power level of 120 dB. If the flow is choked, then W becomes:

$$W = \eta \frac{1}{2} \dot{m} c^2 \quad (117)$$

where c is the speed of sound. Since η can be estimated for single and multiphase flow [32], the sound power level can be easily calculated, not only for pressure reducing devices but also for pipe flow. Attenuation due to friction and temperature changes can be calculated from pipe flow equations in a more detailed manner. SuperChems calculates the sound power level at every axial location for flow piping for single and multi-phase flow.

If the mass flow is divided by n burner flow sources, then the sound power level for a single source becomes:

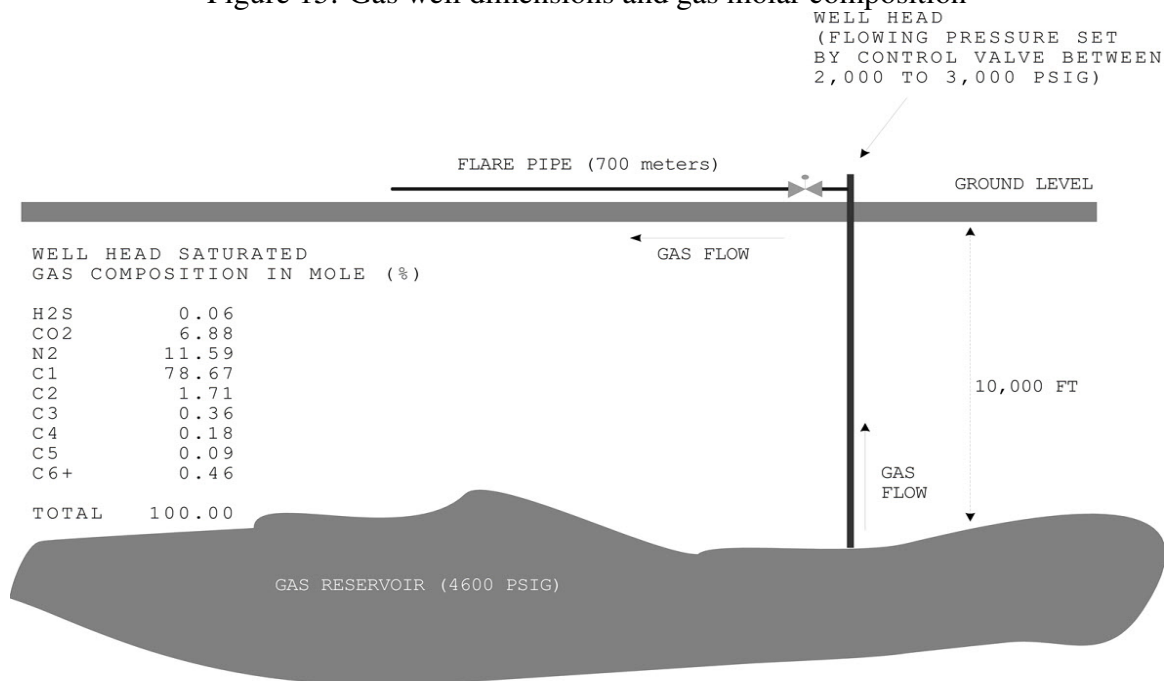
$$L_{W,s} = 10 \log_{10} \left[\frac{\eta_s}{2n} \dot{m} u_s^2 \right] + 120 \quad (118)$$

where η_s is the acoustic efficiency of the flow from each burner, and u_s is the flow velocity from each burner.

Interestingly enough, the incident thermal radiation from each small jet is cumulative for a receptor,

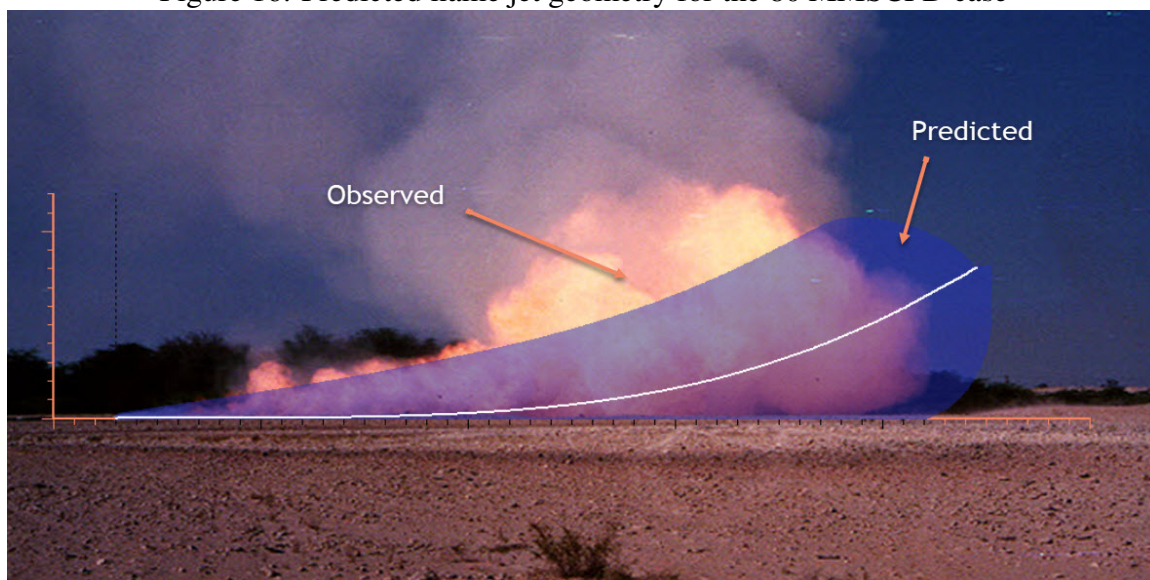
³Sound power level can be converted to sound pressure level [32].

Figure 15: Gas well dimensions and gas molar composition



Source: Arthur D. Little

Figure 16: Predicted flame jet geometry for the 80 MMSCFD case



Source: SuperChems® Expert

but the noise level depends on $\log_{10}$ and the number of jets or sources:

$$L_{W,T} = L_{W,s} + 10 \log_{10}(n) = 10 \log_{10} \left[\frac{\eta_s}{2n} \dot{m} u_s^2 \right] + 120 + 10 \log_{10}(n) \quad (119)$$

where $L_{W,T}$ is the total sound power level in decibels, $L_{W,s}$ is the sound pressure level of one jet or source, and n is the number of jets or sources.

Equations 116 and 119 demonstrate that the reduction in noise for multiple burner configuration is predominantly due to the reduction in acoustic efficiency and velocity of the mass flow in a single burner source:

$$\Delta L_{W,T} = 10 \log_{10} \left(\frac{\eta u^2}{\eta_s u_s^2} \right) \quad (120)$$

Additional features provided by SuperChems include the calculation of required minimal gas purge rate, conversion and destruction efficiencies, as well as equipment surface temperatures.

11.2 Conversion and Destruction Efficiencies

Because SuperChems can provide detailed combustion calculations as the flame jet/flare path is integrated, conversion and destruction efficiencies are easily quantified. Once the user identifies the fuel(s) in the flame jet/flare source fluid, the conversion efficiency is automatically quantified as a function of location in the flame jet/flare. For hydrocarbons:

$$CE = \frac{\dot{m}_{CO_2}}{\dot{m}_{C_{Fuel}}} \quad (121)$$

where $\dot{m}_{CO_2}$ is the mass flow rate of carbon in the CO_2 molecule and $\dot{m}_{C_{Fuel}}$ is the mass flow rate of carbon in the flame jet/flare source fluid or the total mass flow rate of carbon in all the flame jet/flare species.

The destruction efficiency is a better representation of the flame jet/flare ability to convert one or more fuel species to combustion products:

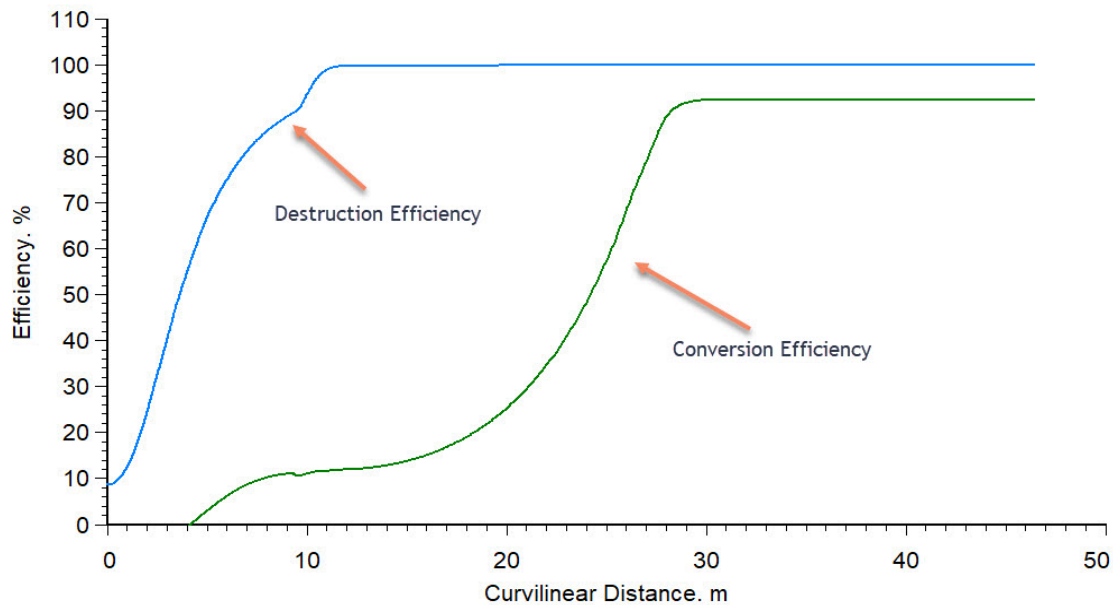
$$DE = 1 - \frac{\dot{m}_{X,jet}}{\dot{m}_{X,Fuel}} \quad (122)$$

where $\dot{m}_{X,jet}$ is the mass flow rate of component X along the flame jet/flare path and $\dot{m}_{X,Fuel}$ is the mass flow rate of component X in the flame jet/flare source fluid. If all of X is converted to combustion products, the destruction efficiency is 1 or 100 %. If none of X is converted, the destruction efficiency is 0.

For Case 1 test data, the conversion and destruction efficiencies of methane and hydrogen sulfide are shown in Figure 17 (also see Figure 11). Note that the starting mole fraction of methane is 85 %, carbon dioxide is 7 %, and nitrogen is 7.9 %.

Figure 18 shows that approximately 2 ppm of nitrogen oxides are present at the end of the visible flare flame tip (also see Figure 11).

Figure 17: Calculated flare methane and hydrogen sulfide conversion and destruction efficiencies for Case 1



Source: SuperChems® Expert

11.3 Gas Purge Rate

The SuperChems® Expert flame jet/flare model includes user options to calculate purge gas requirements for flashback prevention, thermal contraction, and to resist wind penetration, density, and wind effects (physical effects). The purge requirement is reported in velocity and volumetric flow units at standard temperature and pressure conditions.

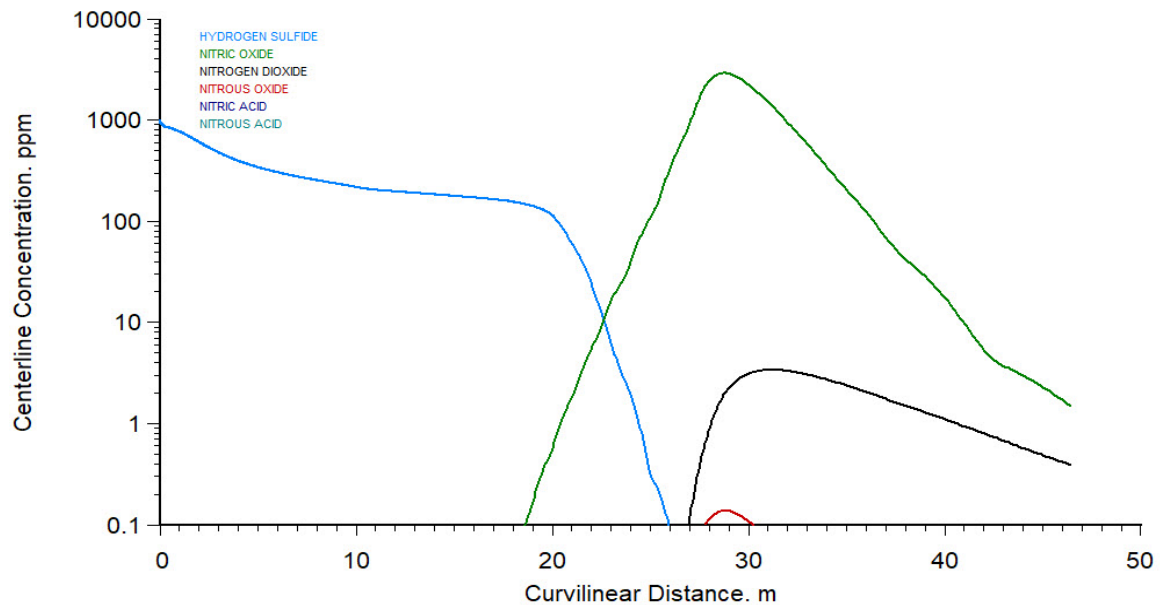
11.3.1 Burn-Back Prevention

To prevent burn-back of the flame into the top of the flare tip, a minimum gas purge rate must be provided. Shore [3] provides an equation for the estimation of the burn-back velocity, or the minimum gas purge rate velocity to prevent flame burn-back into the flare tip:

$$u_{bb} = \frac{\left(\frac{D_i}{M_w}\right)^{0.75} \sqrt{\Delta H_c}}{1500} \quad (123)$$

where u_{bb} is the approximate burn-back velocity in ft/s, D_i is the internal flare tip diameter in inch, ΔH_c is the net heating value of the flared vapor in BTU/lb, and M_w is the molecular weight of the flared vapor or gas.

Figure 18: Calculated flare nitrogen oxides concentrations for Case 1



Source: [SuperChems® Expert](#)

11.3.2 Thermal Contraction

Rapid cooling of flare gases after a hot release can cause a vacuum condition in the flare header network, leading to the intrusion of air into the flare header. Air intrusion into the flare header system can cause deflagrations and/or deflagration to detonation transitions (DDT), a serious explosion risk. This condition is typically mitigated by the timed/staged and controlled introduction of purge gas after a hot release or after other conditions leading to potential vacuum in the flare header network such as rain or draining of the flare knock out drum(s).

[SuperChems® Expert](#) uses a detailed heat transfer method to calculate the required purge gas rate for thermal contraction as a function of time. This is possible in [SuperChems® Expert](#) because detailed volumetric information is already provided for vessels, piping, and flare components including material of construction, geometry or shape, available vapor space volume, insulation, etc. The user merely selects the flare stack and components as well as all other volumes connected to the flare network and then requests a calculation for purge gas requirement as a function of time. This automation enables the user to establish how much purge gas is required, when it is required, and for how long it is required.

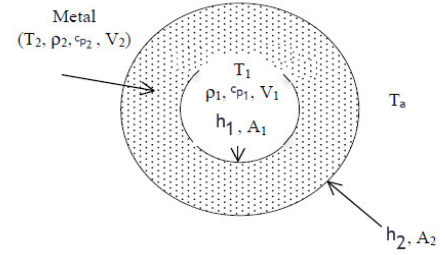
Some operating companies use simpler correlations such as the one proposed by Simpson [33] assuming a cooling rate of 8 °F/min:

$$\dot{V}_{\text{Thermal}} = 0.84 \times V \quad (124)$$

Where $\dot{V}_{\text{Thermal}}$ is the required volumetric gas purge rate in SCFH, and V is the total volume of the flare header network in ft³.

Detailed one dimensional thermal contraction calculations were first proposed by Husa [35] (see Figure 19) from the Amoco Oil Company [36, 34]. A set of differential equations is written for each flare system network volume and integrated in time. The SuperChems® Expert implementation is more detailed than what was originally proposed by Husa [34] because of SuperChems's ability to calculate rigorous thermophysical and transport properties, heat transfer coefficients, and to integrate the equations numerically (see Figure 19).

Figure 19: One dimensional heat transfer



Source: [34]

$$\frac{dT_2}{dt} = \frac{h_a A_2 (T_a - T_2) + h_r A_2 (T_r - T_2) + h_1 A_1 (T_1 - T_2)}{\rho_2 V_2 c_{p,2}} \quad (125)$$

$$\frac{dT_1}{dt} = \frac{h_1 A_1 (T_2 - T_1)}{\rho_1 V_1 c_{p,1}} \quad (126)$$

where t is time from the start of thermal contraction, T_1 is the internal gas temperature at time t , T_2 is the metal temperature at time t , T_a is the surroundings temperature, T_r is the rain temperature (typically 15 °C), h_1 is the internal vapor-wall overall heat transfer coefficient, h_2 is the outside heat transfer coefficient, h_r is the outside rain heat transfer coefficient, h_a ($\simeq 5 \text{ W/m}^2/\text{K}$) is the convective air to vessel wall heat transfer coefficient, A_1 is the inside heat transfer area, A_2 is the outside heat transfer area, ρ_1 is the mass density of the vapor, ρ_2 is the mass density of the metal, V_1 is the internal volume of the vapor, V_2 is the metal volume, $c_{p,1}$ is the specific heat capacity at constant pressure for the vapor, and $c_{p,2}$ is the metal specific heat capacity.

The internal wall-vapor heat transfer coefficient h_1 will mostly be in the natural convection regime and can be calculated using literature correlations that depend on the vessel geometry. For an isothermal vertical plate, the Churchill and Chu [37] correlation can be used for laminar and turbulent natural convection flow:

$$h = \frac{k}{L} N_u \quad (127)$$

$$= \frac{k}{L} \left[0.68 + \frac{0.67 N_{Ra}^{1/4}}{\left(1 + \left[\frac{0.492}{N_{Pr}} \right]^{9/16} \right)^{4/9}} \right] \quad \text{Laminar Flow } N_{Ra} < 10^9 \quad (128)$$

$$= \frac{k}{L} \left[0.825 + \frac{0.387 N_{Ra}^{1/6}}{\left(1 + \left[\frac{0.492}{N_{Pr}} \right]^{9/16} \right)^{8/27}} \right]^2 \quad \text{Laminar and Turbulent Flow} \quad (129)$$

$$N_{Ra} = \frac{g\beta(T_w - T)L^3\rho^2c_p}{\mu k} \quad (130)$$

$$N_{Pr} = \frac{\mu c_p}{k} \quad (131)$$

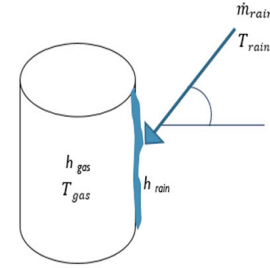
where N_u is the Nusslet number, N_{Pr} is the Prandtl number, N_{Ra} is Rayleigh's number, k is the gas thermal conductivity in W/m/K, L is the vertical plate characteristic length in m, $g \simeq 9.8 \text{ m/s}^2$ is the gravitational constant, β is the volumetric expansion coefficient in K^{-1} , ρ is the gas mass density in kg/m^3 , c_p is the gas specific heat capacity at constant pressure in J/kg/K , and μ is the gas viscosity in Pa.s . Different forms of N_{Ra} are available for different geometries such a horizontal cylindrical geometries and/or spherical geometries.

The rain to vessel wall heat transfer coefficient h_r can be estimated from a recent correlation [39, 38] which depends on the rain intensity in in/hr:

$$h_r = 33.87z_{\text{rain}} \quad (132)$$

where h_r is the rain to vessel wall heat transfer film coefficient in $\text{W/m}^2/\text{K}$ and z_{rain} is the rain intensity in inches/hr. Equation 132 assumes the wind speed is adjusted to yield a 60 degrees rain angle with respect to vertical and a rain temperature of 15°C . This rain liquid film heat transfer coefficient is also used when modeling the in-breathing requirements for storage tanks and vessels when exposed to rain.

Figure 20: Rain to wall film heat transfer coefficient



11.3.3 Physical Effects

The most widely used procedure/correlation for calculating the minimum purge requirements to protect against physical effects (wind, density, and diffusion) is the method originally developed by Husa [34, 35, 36]. The Husa correlation is reported to be valid for stacks up to at least 60 inches in diameter [34]. The required minimum purge rate is provided by Equation 133:

$$\dot{V}_{\text{Physical}} = 0.07068 K D^{3.46} \left(\frac{1}{y}\right) \ln\left(\frac{20.9}{x_{O_2}}\right) \quad (133)$$

$$K = \sum_i^n c_i^{0.65} K_i \quad (134)$$

$$K_i = \exp[0.065(29 - M_{w,i})] \text{ For } M_{w,i} \leq 29 \quad (135)$$

$$= \frac{1}{\exp[0.065(29 - M_{w,i})]} \text{ For } 29 < M_{w,i} < 58 \quad (136)$$

$$= -6.586 \text{ For } M_{w,i} \geq 58 \quad (137)$$

where $\dot{V}_{\text{Physical}}$ is the purge rate in SCFH, D is the flare stack diameter in inches, y is the fluid column depth at which the oxygen concentration is to be predicted in ft, x_{O_2} is the volume % of oxygen, c_i is the volume fraction of gas component i , K is the overall gas buoyancy constant, and K_i is the gas buoyancy constant for component i .

A K_i value for nitrogen is 1.067 under no wind conditions and should be set to 1.707 (for wind speeds between 15 and 20 mph). If the overall value of K is calculated to be less than that of nitrogen, it should be set to the nitrogen value [34].

For flaring of gases with an average molecular weight of 4 or less, x_{O_2} is set to 3 %. For flaring of gases with an average molecular weight of 6 or less, x_{O_2} is set to 4 %. For flaring of gases with an average molecular weight of 8 or less, x_{O_2} is set to 5 %. Most flare purge gas applications will require an x_{O_2} of 6 % for flaring gases with molecular weights > 8 .

12 Conclusions

This paper provides a method for incorporating detailed combustion reactions for flame jet and flare modeling. Combustion along the flame jet/flare curvilinear path is calculated using chemical equilibrium by direct minimization of the Gibbs free energy. Integrating detailed combustion reactions with two-dimensional similarity profiles integral methods for flow provides much better estimates in both momentum and buoyancy dominated regions of the flame jet or flare.

Added benefits from the incorporation of detailed combustion reactions include the identification, quantification, and dispersion of combustion species and pollutants as well as estimates of destruction and combustion efficiencies along the flame jet path.

The new method is implemented in [SuperChems[®] Expert](#) and validated against actual large scale test data for natural gas and hydrogen with excellent agreement between predictions and measurements using literature established entrainment parameters without any adjustment or tuning.

13 Recommended Additional Reading

Please refer to additional [ioMosaic[®]](#) publications in the fire modeling series for more details on understanding thermal radiation modeling and atmospheric transmissivity, the calculation of geometric view factors for solid flame geometries, the calculation of flame emissive power and fraction radiated for pool fires and flame jets/flares, the calculation of short duration exposure to thermal radiation from fireballs and vapor cloud fires, the dynamics of pressure relief under fire exposure, and estimated time to failure or yield for vessels exposed to internal heating by runaway reaction(s) and/or external heating by fire. These technical publications can be made available upon request.

14 References

- [1] G. Ooms. A new method for the calculation of the plume path of gases emitted by a stack. *Atmospheric Environment*, 6:899, 1972.
- [2] AEA. Initial review of air quality aspects of the buncefield oil depot explosion. Technical Report AEA/ENV/R/2168 Issue 1, Department for Environment, Food and Rural Affairs, the Scottish Executive, the Welsh Assembly Government and the Department of the Environment in Northern Ireland, 2006.
- [3] D. Shore. Making the flare safe. *Journal of Loss Prevention in the Process Industries*, 9(6):363–381, 1996.
- [4] C. R. Shaddix and T. C. Williams. Soot: Giver and taker of light. *Americal Scientist*, 95:232–239, 2007.
- [5] K. S. Mudan and P. A. Croce. Fire hazard calculations for large open hydrocarbon fires. In *The SFPE Handbook of Fire Protection Engineering, Section 2, Chapter 4*. NFPA, 1988.
- [6] S. V. Patankar, D. K. Basu, and S. A. Alpay. Prediction of the three-dimensional velocity field of a deflected turbulent jet. *ASME J. Fluids Eng.*, 99(4):758, 1977.
- [7] B. E. Launder and D. B. Spalding. The numerical prediction of turbulent flows. *Computer Methods in Applied Mechanics and Engineering*, 3:269, 1974.
- [8] S. Benteboula and E. Studer. Integral models for high pressure hydrogen - methane releases. *3rd International Conference on Hydrogen Safety*, pages 1–12, 2009.
- [9] J. C. Kaimal, J. C. Wyngaard, D. A. Haugen, O. R. Cote, and Y. Izumi. Turbulence structure in the convective boundary layer. *J. Atmos. Sci.*, 33, 1976.
- [10] G. Ooms, A. P. Mahieu, and F. Zelis. The plume path of vent gases heavier than air. In *First International Symposium: Loss Prevention and Safety Promotion in The Process Industries.*, 1974.
- [11] I.W. Ekoto, A.J. Ruggles, L.W. Creitz, and J.X. Li. Updated jet flame radiation modeling with buoyancy corrections. *International Journal of Hydrogen Energy*, 39(35):20570–20577, 2014.
- [12] Y. Kamotani and I. Greber. Experiments on a turbulent jet in a cross flow. *AIAA Journal*, 10(11), 1972.
- [13] J. F. Keffer and W. D. Baines. The round turbulent jet in a cross wind. *Journal of Fluid Mechanics*, 15(4), 1963.
- [14] J. Havens. A dispersion model for elevated dense gas jet chemical releases - volume i. NTIS PB88-202387, U.S. Environmental Protection Agency, 1988.
- [15] W. J. Astleford, T. B. Morrow, and J. C. Buckingham. Hazardous chemical vapor handbook for marine tank vessels. NTIS AD-A128 768, Southwest Research Institute, 1983.
- [16] D. B. Pfenning, S. B. Millsap, and D. W. Johnson. Comparison of turbulent jet model predictions with small-scale pressurized releases of ammonia and propane. In *International*

- Conference on Vapor Cloud Modeling*, pages 81–99. CCPS, AIChE, 1987.
- [17] G. T. Kalghatgi. The visible shape and size of a turbulent hydrocarbon jet diffusion flame in a cross-wind. *Combustion Science and Flame*, 52:91, 1983.
- [18] G. T. Kalghatgi. Lift-off heights and visible lengths of vertical turbulent jet diffusion flames in still air. *Combustion Science and Flame*, 41:17–29, 1984.
- [19] W. R. Hawthorne, D. S. Weddell, and H. C. Hottel. Mixing and combustion in turbulent gas jets. In *Third Symposium on Combustion, Flame and Explosion Phenomena*, page 266. Williams and Wilkins, 1949.
- [20] T. A. Brzustowski. A new criterion for the length of a gaseous turbulent diffusion flame. *Comb. Sci. and Tech.*, 6:313, 1973.
- [21] M. A. Field, D. W. Gill, B. B. Morgan, and P. G. Hawksley. The effects of explosives in the process industries. Combustion of pulverized coal, 1967. BCURA.
- [22] A. A. Putnam and C. F. Speich. A model study of the interaction of multiple turbulent diffusion flames. In *Ninth Symposium (International) on Combustion*, page 884. Academic Press, 1963.
- [23] A. L. Suris, E. V. Flankin, and S. N. Shorin. Length of free diffusion flames. *Combust. Explos. Shock Waves*, 13(4):41, 1977.
- [24] O. K. Sonju and J. Hustad. An experimental study of turbulent jet diffusion flames. Technical Report STF15-A84048, Royal Norwegian Council for Scientific and Industrial Research, 1984.
- [25] M. A. Delichatsios. Transition from momentum to buoyancy-controlled turbulent jet diffusion flames and flame height relationships. *Combustion and Flame*, 92(4):349364, 1993.
- [26] D. B. Pratt and W. D. Baines. Profiles of the round turbulent jet in a cross-flow. In *ASCE HY6*, page 53, 1967.
- [27] V. O. Hoehne and R. G. Luce. The effects of velocity, temperature and molecular weight on flammability limits in wind-blown jets of hydrocarbon gases. Preprint 56-70, API, 1970.
- [28] T. A. Brzustowski. Turbulent diffusion models iii: The buoyant flame in a cross-wind. In *Proc. Vth Can. Conf. App. Mech., U. of New Brunswick*, 1975.
- [29] T. A. Brzustowski. Predicting radiant heating from flares. Technical Report EE.15ER.71, Esso Research and Engineering, 1971.
- [30] R. E. Schwartz and J. W. White. Predict radiation from flares. *Chemical Engineering Progress*, pages 42–49, July 1997.
- [31] I. Ekoto, B. Houf, A. Ruggles, J. Li, and L. Creitze. Updated jet flame radiation modeling with buoyancy corrections. In *International Conference on Hydrogen Safety*. Sandia National Laboratories, September 2013.
- [32] G. A. Melhem. Estimate vibration risk for relief and process piping. In *9th Global Congress on Process Safety*. CCPS Center for Chemical Process Safety, AIChE, 2013.
- [33] CCPS/AIChE H. G. Fisher and G. A. Melhem (Editors). *Guidelines for Pressure Relief and*

Effluent Handling Systems. Wiley, New York, 2nd edition, 2016.

- [34] D. E. Fritz. Calculating purge gas rates for flare and blowdown stacks. Technical Report M84-87, Amoco Oil Company, July 1984.
- [35] H. W. Husa. Purging requirements of large diameter flares. In *Fire/Safety Engineering Subcommittee Fall Meeting*. API, September 1977.
- [36] D. E. Fritz. Flare purge gas rates and flashback protection. Technical Report M82-140, Amoco Oil Company, December 1982.
- [37] Stuart W. Churchill and Humbert H.S. Chu. Correlating equations for laminar and turbulent free convection from a vertical plate. *International Journal of Heat and Mass Transfer*, 18(11):1323–1329, 1975.
- [38] D. Chakra, G. Melhem, and R. Willey. Dynamic modeling of inbreathing requirements for low pressure storage tanks. In *13th Global Congress on Process Safety*. American Institute of Chemical Engineers, March 2017.
- [39] D. Brooks. Thermal inbreathing requirements of low pressure storage tanks at elevated temperatures. In *Spring DIERS Users Group Meeting*. American Institute of Chemical Engineers, May 2012.

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