 DOI: 10.3217/978-3-85125-883-7-11

ON HEAT TRANSFER COEFFICIENTS AND TEMPERATURE DISTRIBUTION IN LONGITUDINALLY VENTILATED TUNNEL FIRES

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ABSTRACT

Various approximate methods, and guidelines, are followed by tunnel-ventilation designers in the process of sizing the ventilation system. Of particular importance are the heat transfer coefficients used in prediction of the temperature distribution during a fire event. This strongly affects the ventilation exerted trust, and induces a chimney-effect pressure in sloped tunnels. For this purpose, a one-dimensional numerical solution approach is used in this work to evaluate their values. In addition, processing of a selected tunnel-fire-test from the literature data is also used in order assess the heat transfer coefficients values from realistic fire-tests. The results are discussed for final conclusions.

Keywords: longitudinal tunnel ventilation sizing, fire scenario, transient heat transfer, convection, radiation, conduction, heat transfer coefficients, chimney effect

Nomenclature

a – thermal diffusivity (m^2/s)

A– area of tunnel cross-section *(m^2)*

c_p– specific heat of the gas mixture, at a constant pressure (J/kgK)

D_h – hydraulic diameter, *4A/P*

f_D – Darcy friction coefficient (-)

F – view factor for radiative heat transfer,

h – coefficient of heat transfer ($W/m^2 K$)

$\dot{m}$ – mass flow rate (kg/s)

n – exponent in the Nusselt number correlation

Nu – Nusselt number, hD_h/k

Pr – Prandtl number, ν/a

P– perimeter of the tunnel cross-section (m)

p – pressure (Pa)

$\dot{Q}$ – fire heat release rate *(W)*

Re – Reynolds number, uD_h/ν

s – tunnel slope (%)

St – Stanton number, $Nu/(RePr)$

T– temperature (K)

u – averaged velocity of the gas mixture (m/s)

y – *wall-normal coordinate*

Greek symbols

λ – heat conduction coefficient (W/mK)

ϕ – heat flux transferred to the tunnel wall (W/m^2)

ρ – average density of the gas mixture (kg/m^3),

ν – kinematic viscosity (m^2/s),

ε – coefficient of emission,

σ– Stefan–Boltzmann constant (W/m^{2}K)

Subscripts

a – air

avg –averaged value over the tunnel cross-section

c – convection

cc – convection + conduction

ch – chimney

cr – convection + radiation

crc – convection + radiation + conduction

m – mean value over the tunnel length

r – radiation

w – wall

1. INTRODUCTION

The critical regime for longitudinal ventilation system sizing, appears at approx. 15 min after fire onset, when the evacuation of tunnel users is over and fire-suppression and extinction by the fire brigade commences. Of particular importance is the ability to calculate the distribution of the gas-average temperature (at cross section) along the tunnel, at that point in time. The gas-average temperature distribution affects ventilation sizing by: gas acceleration along the tunnel that increases wall friction losses; the chimney-effect is induced in sloped tunnels; the ventilation thrust deteriorates due to a reduced gas density, i.e. mass flow through the fans.

Attention in this paper is given to the heat transfer coefficients, their proper definition, calculation and use in ventilation sizing, which seems overlooked in literature. Thus, a topic where the designer might encounter some difficulty. To this end, a one-dimensional numerical approach is used, preceded by an assessment on proper use of literature data and heat transfer formulae. In addition, an assessment resulting from processing the literature-available measurement-data obtained during a real-scale-tunnel fire-test, is given.

The energy equation of the gas stream in the tunnel can be written in the form [1], [2]:

$$-\dot{m}c_p \frac{1}{P}\frac{dT_{avg}}{dx} = \dot{q}_c'' + \dot{q}_r'' \qquad (1)$$

where $\dot{q}_c'' = h_c(T_{avg} - T_w)$, and $\dot{q}_r'' = F\sigma(\epsilon_g T_{avg}^4 - \alpha_g T_w^4)$, are local convective and radiative heat transfer rate fluxes from the gas to the tunnel wall, respectively, in [W/m^2], [2],[3], whereas conduction heat flux inside the wall, in the wall-normal direction y, is: $\dot{q}_{cond}'' = -\lambda(\partial T_w/\partial y)$. We can define heat transfer coefficients accounting for: h_c - convection only, h_r - radiation only, the h_{cr} - joint convection-radiation (each related to the gas-average to wall-surface temperature difference), and an overall-heat-transfer coefficient (convection-radiation-conduction), h_{crc} (related to gas-to-rock massive temperature diff.), where the undisturbed wall (rock-mass) temperature T_∞ is used. Thus, these definitions read:

$$h_c = \frac{\dot{q}_c''}{T_{avg}-T_w},\ h_r = \frac{\dot{q}_r''}{T_{avg}-T_w},\ h_{cr} = \frac{\dot{q}_c''+\dot{q}_r''}{T_{avg}-T_w},\ h_{crc} = \frac{\dot{q}_c''+\dot{q}_r''}{T_{avg}-T_\infty}, \qquad (2)\ (a, b, c, d)$$

For example, in the literature approaches, the following approximate solutions for the temperature distribution and the chimney-pressure-effect, for use with $h_{crc,m}$ only, can be found [2], [4],[5],[6], in the form:

$$T_{avg}(x,t) = T_\infty + [T_{max} - T_\infty]e^{-\frac{h_{crc,m}Px}{\dot{m}_a c_p}} \qquad (3)$$

$$T_{max} = T_a + \eta_r \frac{HRR}{\dot{m}_a c_p} \qquad (4)$$

$$\Delta p_{ch} = -\frac{\rho_a g s \dot{m}_a c_p}{100 h_{crc,m} P} ln \frac{\eta_r \frac{HRR}{\dot{m}_a c_p} e^{-\frac{h_{crc,m}P}{\dot{m}_a c_p}L} + T_a}{T_a + \eta_r \frac{HRR}{\dot{m}_a c_p}} \qquad (5),$$

where: *HRR* is the heat-release-rate of the fire, *L*-affected length, *P*-tunnel perimeter, *s*-tunnel slope, η_r (in the range: *2/3 - 3/4*) is the portion of the *HRR* available past the local (flame-to-wall) radiative *HRR* loss, and the index *m* is added in this work to the overall-heat-transfer coefficient h_{crc} to denote its *mean value over the considered fire-affected length L*. One can assume the cold flow air temperature T_a and T_∞ (rock massive undisturbed temperature) to be approx. equal - these were further denoted jointly as T_0.

2. ANALYSIS OF DATA

The following input data, corresponding to a typical horseshoe-shape cross-section 2-lane highway road-tunnel were used as a generic-tunnel numerical example, Table 1.

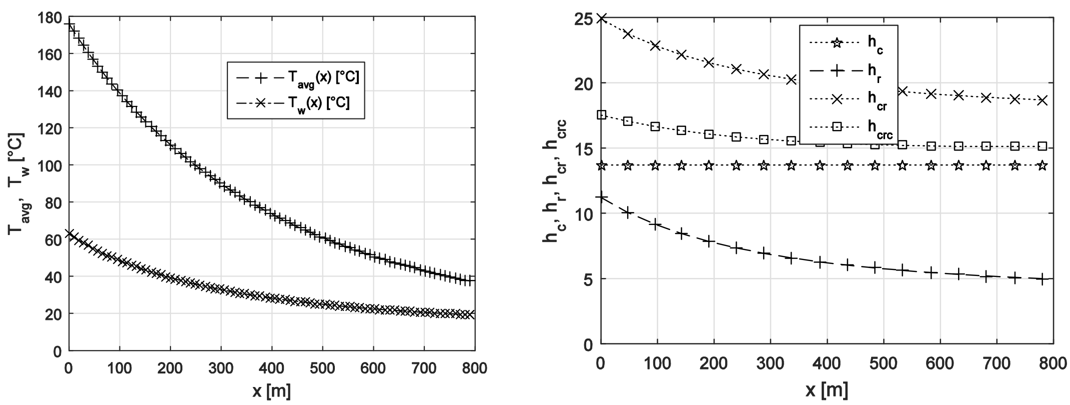
Table 1: Input data for a generic road-tunnel case computed numerically

Tunnel cross section area A_t [m^2]	Considered tunnel length L_t [m]	D_h Tunnel hydraulic diameter[m]	Inflow air velocity: u_{cr} [m/s]	HRR_{max} [MW]	Effective Darcy friction factor f_D	Tunnel wall thermal properties
55.1	*800*	*7.7*	*3*	*50*	*0.0275*	λ=*1.65* W/mK ρ=*2400* kg/m^3 *c = 920* J/kgK

The *HRR(t)* time-dependence is taken from [2]: a linear increase over *10* min time (*0* to HRR_{max}), followed by a constant max. value over *10* min time, and a linear decrease back to zero over additional *10* min. A numerical simulation time of *15* min from the fire-onset was selected for the computation presented in sec.2.1., as discussed in the introduction.

2.1. One-dimensional numerical solution of the generic example

A transient distribution of tunnel's cross-section flow-averaged variables (density, temperature, pressure, velocity in the tunnel-axis direction) are numerically computed by solving 1D continuity, momentum and energy differential equations of flow using a specialized software package [7], without simplifications required for simplified solutions. Thus, the results can be considered reliable, and its purpose is to serve designers in sizing of the ventilation systems. To this end, it computes the transient temperature distribution of the tunnel wall numerically. The wall temperature is considered as locally dependent on wall normal coordinate and time only, but the solution is carried out independently at numerical locations spread along the tunnel axis *x,* spanning the considered tunnel length. The details of the governing numerical model, discretization and the solution procedures can be found in [7]. The heat convection coefficient in this numerical procedure is calculated using Petukhov equation with the user-input of Darcy (Moody-chart) friction factor f_D value. The heat radiation is accounted for by means of an equivalent heat transfer coefficient h_r, eq.(2.b). The variables are obtained as cross-section averages, functions of time and the *x*-distance. The computed distributions are given in Fig.1.



a) b)

Fig.1. Numerically computed results for the generic-tunnel example in Table1: a) Gas-average and wall-average temperatures, $T_{avg}(x)$, $T_w(x)$, b) Heat transfer coefficients: h_c, h_r, h_{cr}, h_{crc}

In Table 2. the mean values for the heat transfer coefficients are given. They are defined as:

$$h_{*,m} = \frac{\int_0^L h_*(T_{avg}(x)-T_w(x))Pdx}{\int_0^L(T_{avg}(x)-T_w(x))Pdx}\,,\quad h_* \text{ is: } h_c(\text{x}),\, h_r(\text{x}), \text{ or } h_{cr}(x) \quad (6)$$

$$h_{crc,m} = \frac{\int_0^L h_{crc}(T_{avg}(x)-T_0)Pdx}{\int_0^L(T_{avg}(x)-T_0)Pdx} \quad (7)$$

Table 2: Computed mean values of heat-transfer coefficients for the generic road-tunnel numerical case

HRR	$h_{c,m}$	$h_{r,m}$	$h_{cr,m}$	$h_{crc,m}$
50 MW	*13.7*	*7.53*	*21.33*	*16.1*

2.2. Literature predictions for the friction factor f_D and the convective coefficient h_c

Comparisons given bellow are for the same generic-tunnel example used in sec.2.1. (fluid properties: $Pr=0.7$, $T_0=288K$, $T_m=98°C=372K$: $\nu=2.2986\cdot10^{-5}$ m²/s, $\lambda_{am}=0.0316$ W/mK). The velocity and the resulting Re numbers, obtained by use of linear-mean or true mean value for gas temperature (~98°C vs ~70°C, respectively), are: $u_m = 3.87 \mid 3.57$, respectively, and Re: $1.31\cdot10^6 \mid 1.33\cdot10^6$. The cold airstream approaching the fire has a Re value: $1.57\cdot10^6$.

Civil engineering literature provides values for concrete tunnel-wall absolute roughness: ~3-9 mm, thus the Darcy friction factor values, at the Re number given above (Re≈$1.33\cdot10^6$) by Moody-chart based predictions are: $f_D = 0.011 \mid 0.016 \mid 0.020$, for: smooth, minimally rough, maximally rough tubes, respectively. However, the common understanding [8], [9] for cast-concrete-lining traffic-tunnels is that the effective value of the Darcy friction factor f_D is of the order ≈0.025-0.030, based on real-scale experiments, at Re numbers of interest (critical velocity for smoke control, or higher). A value of $f_D=0.0275$ is selected and used further as input-data value for all the calculations in this paper, of the generic-tunnel example-case. With rock / sprayed-concrete tunnel linings, f_D can reach much higher values, which will be discussed later.

2.2.1. Smooth-tube tunnel convection coefficient $h_{c,s}$ values

The following values can be obtained by using literature correlations [3], [10]:

General formula: $h_{c,s} = 0.0265\cdot Re_m^{0.8}\cdot Pr^{0.333}\cdot \lambda_{am}/D_h = \ldots = 7.58$ W/m²K (8)

Sieder-Tate formula: $h_{c,s} = 0.027\cdot Re_m^{0.8}\cdot Pr^{0.3}\cdot \lambda_{am}/D_h\cdot(\nu_m/\nu)^{0.14} \ldots = 8.11$ W/m²K (9)

Newman formula: $h_{c,s} = 0.026\cdot Re_m^{-0.2}\cdot(1 + (D_h/1500)^{0.7})\cdot1.2\cdot1010\cdot u_m = 7.48$ W/m²K (10)

Petukhov: $h_{c,s} = \dfrac{\frac{f_D}{8}c_p\rho u}{1.07+12.7\left(Pr^{\frac{2}{3}}-1\right)\sqrt{\frac{f_D}{8}}} = \ldots = 5.03$ W/m²K (11)

2.2.2 Rough-wall tunnel heat convection coefficient $h_{c,r}$

The tunnel-ventilation designer can either use the Petukhov formula, with the appropriate friction factor f_D value, which here the previously adopted value is $f_D=0.0275$, and obtain: $h_c=13.7$ W/m²K. Otherwise, one can use a specific formula by Norris [10], based on forced-convection experiments in rough-wall tubes, eq. (12). This formula gives a prediction for rough-tube forced convection coefficient as a function of its value in the smooth-tube flow at the same Re number, and the ratio of Darcy friction factor values (for rough vs. smooth tube-wall), in the form:

$$h_{c,ro} = h_{c,sm}\left(\frac{f_{D,ro}}{f_{D,sm}}\right)^n, n = 0.68\cdot Pr^{0.215} \quad (12)$$

with: $h_{c,sm}$ - the convective heat transfer coefficient for smooth-tube flow, $f_{D,ro}/f_{D,sm}$ - a ratio of friction factors (both at the same Re number), n - the exponent depending on the fluid's Prandtl number (here further taken as 0.7, thus $n = 0.6298$). With an important note [10] that there is an upper limit for the formula eq.(12), occurring at the value of roughness which produces the Darcy friction factor 4 times higher than its smooth-tube-flow variant at the same Re number. A further increase of tube roughness above that value does not result in increase of h_c. This means that for fluid properties approx. equal to air ($Pr=0.7$), a maximum increase factor for h_c due to friction (*rougher walls*) is of the order: ·2.394 or by 140%, compared to the smooth wall. For the generic-tunnel used here, such limit would be encountered at $f_{D,ro} = 4\cdot f_{D,sm} \approx 0.044$. Depending on the formula one adopts to calculate the smooth tube case (*the Sieder-Tate, the Petukhov, or the Newman formula*), the following h_c value result for the *rough-wall* case, using the generic-tunnel example input-data: $h_{c,ro} = \underline{14.45 \mid 8.97 \mid 13.32}$ W/m²K, respectively, by the use of Norris formula, eq.(12). It is worth noting the effect that the surface roughness, and the selected formula, plays on h_c, within the range of f_D values relevant in tunnel ventilation. Using input data for the generic-tunnel example, the following result is obtained, Fig.2.

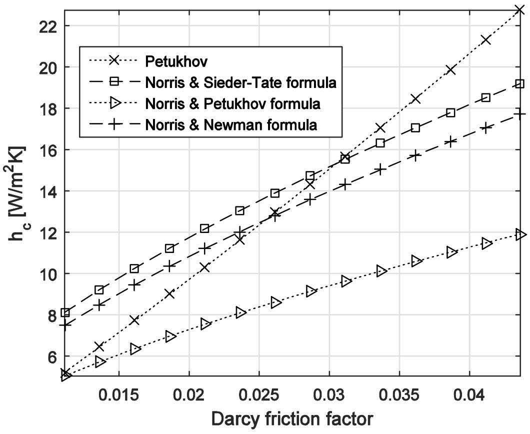


Fig.2. Effect of the Darcy friction factor f_D on h_c [W/m^{2}K] value in the relevant f_D range according to the limit by Norris ($f_{D,sm}$ - $4f_{D,sm}$) [10], for the generic-tunnel example-data ($f_{D,sm}$=0.0111, u=3 m/s, D_h=7.7m, HRR=50MW, $T_{ref}=0.5(T_{avg,max}+T_0)$=372K)

A different formula for smoke-to-ceiling convection coefficient h_c in fire-engineering was used by Zhao et al. [11], to evaluate convection between hot smoke layer and walls but it should be used with a modified *wet perimeter* value of the smoke layer only and will not be discussed here.

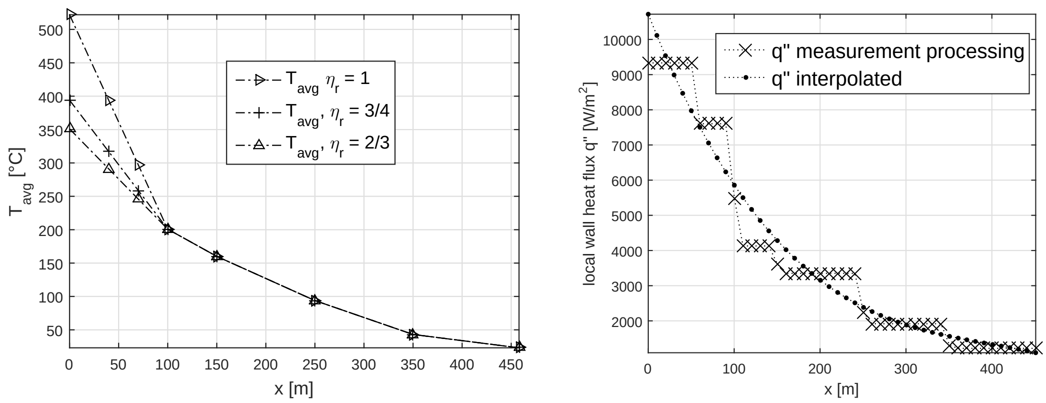
2.3. Analasys of a real-scale tunel-fire test data

The data selected correspond to literature-available tunnel-fire test in Norway, (*"Runehamar" tunnel*) [5]. Conclusions from processing the *70*MW and *120*MW *HRR*-value data will be analysed here only. The data correspond to approx. *15-20* min of time from the fire onset. For the *70* MW case, the relevant reported data [5]: HRR_{max}=70MW, *P(0-53m)=22m, P(53-L)=27m and* $D_h \approx 7m$, intial $u_0 \approx$ 3.15 m/s but reduces upon fire development to u_0~2.5m/s, fluid properties at T_m=436 K ($\nu = 30.31 \cdot 10^{-6}\ m^2/s$, $\lambda_{am} = 0.0363$ W/mK), $f_{D,sm}$ = 0.01145, $f_{D,ro} = 0.0585$ (thus > $4f_{D,sm} = 0.04575$), and estimated wall-temperature in fire-near area >~100°C. The very high relative roughness of the walls (average absolute roughness over *300mm*) where these tests were taken, resulted in a reported Darcy friction factor value: $f_{D,r}$=0.0585. Data from these tests are available as temperatures measured at two different heights above road surface, T_L and T_C, along the tunnel length, which allowed to calculate the estimate for the average-temperature $T_{avg}(x)$ in this work.

2.3.1. Analysis of the fire-test data and the heat transfer coefficients h_c, h_r, h_{cr}, h_{crc}

The following values for h_c, as the literature-based prediction values, in *70* MW fire-test were obtained: Petukhov (*smooth tube* vs *rough tube*) case: *8.11* │*36.29* W/m^{2}K; Norris (with Petukhov for $h_{c,sm}$): *19.41* W/m^{2}K, and Norris (with Sieder-Tate for $h_{c,sm}$): *21.47* W/m^{2}K. The rough-wall value obtained using Petukhov formula is hardly valid, since the limit $f_{D,ro}$ =0.0585 > $4f_{D,sm}$ = 0.04575, given by Norris eq.(10), is met. Thus, the latter two values obtained by use of Norris formula are recommended: 19.41│21.47 W/m^{2}K. For the overall heat transfer coefficient $h_{crc,m}$, a best-fit interpolation value of ~*25* W/m^{2}K was proposed by Ingason in [5], along with use of η_r value *2/3*, for use in the approximate-type solutions, eqs.(2)-(3), [2], [5].

Using the above computed $h_{c,ro}$ value *21.47* W/m^{2}K further, the temperature-data processing was carried out to estimate the heat transfer coefficients: h_c, h_r, h_{cr}, h_{crc}, their distribution along the tunnel, and their mean values, at the time of the measurements (*~15-20* min from fire onset). Ceiling height in this test-tunnel was ≈6m, and temperature measurements were done at: *1.8*m (T_L) above road and *0.3*m under ceiling (T_C). Assuming local gas velocity to adhere to the relationship $u=u_0T/T_0$ [1],[2], using ideal-gas equation of state, $\rho=\rho_0T_0/T$, assuming a roughly linear variation of gas temperature with height, one can obtain an interpolated value estimate for $T_{avg}(x)$. The temperature measurement was not available from these tests near the maximum-temperature location (fire-site). But, since $T_{avg}(x)$ must initiate from a thermodynamically-constrained $T_{avg,max}$ value, using a recomended radiative loss factor $\eta_r = 2/3$, $T_{avg,max} = T_0 + \frac{\eta_r \cdot HRR}{\dot{m} c_p}$, the interpolated T_{avg}(x) distributions are obtained from the fire-site downstream, and shown in Fig.3.a using $\eta_r = 1 \,|\, 3/4 \,|\, 2/3$ value.



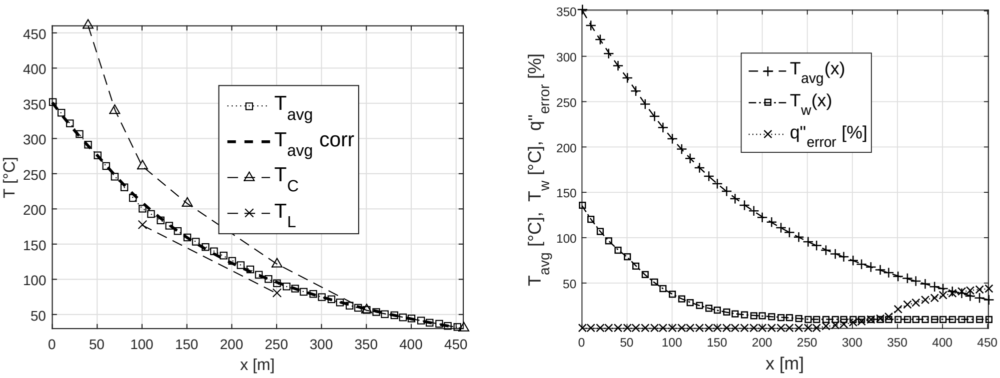
a) b)

Fig.3. The *70* MW fire-test data processing: a) interpolations for $T_{avg}(x)$, step (°1) of the procedure below, b) local heat flux $\dot{q}''$ processing, steps (°2) and (°3).

Using further $\eta_r = 2/3$, data were further processed according to the following algorithm:

1) The rate of change of interpolated $T_{avg}(x)$ with x is determined numerically: dT_{avg}/dx;
2) The local heat flux, $\dot{q}''$ [W/m²] is computed as: $\dot{q}''(x) = \rho_0 u_0 A_t \frac{dT_{avg}}{dx}$ [W/m²];
3) An interpolated (smooth) distribution of *measured* $\dot{q}''_{meas}$ is obtained by a polynomial interpolation through the $\dot{q}''(x)$ result of step (°2), Fig.3.b, and used further;
4) A corrected (smooth) distribution of $T_{avg}(x)$ is obtained by integrating back the $\dot{q}''_{meas}$ with respect to x, and used further, Fig.4.a;
5) The average wall temperature T_w at x, which must comply to: $T_0 < T_w(x) < T_{avg}(x)$ is reconstructed iteratively by an algorithm which minimises the difference between the local $\dot{q}''_{meas}(x)$ (experiment-data based heat-flux) and the calculated local heat flux $\dot{q}''_{calc}(x)$ determined from: $\dot{q}''_{calc} = h_c(T_{avg} - T_w) + F_{12}\sigma(\epsilon_g T_{avg}^4 - \alpha_g T_w^4)$.

The results for the $\dot{q}''(x)$, $Tavg(x)$, and the iteratively-computed $T_w(x)$, along with a relative-error in $T_w(x)$-determination, expressed as a relative difference between $(\dot{q}''_{meas} - \dot{q}''_{calc})/\dot{q}''_{meas}$ [%], are plotted together in Fig.4.b.



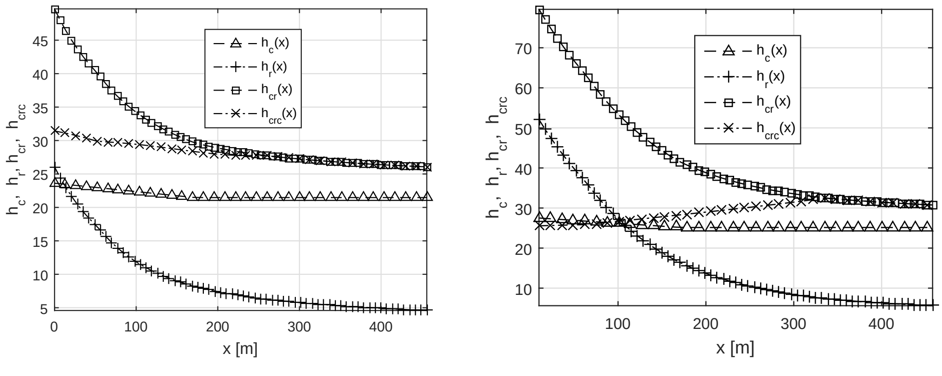
a) b)

Fig.4. *70* MW fire-test processed data: a) Interpolated $T_{avg}(x)$, using $\eta_r=2/3$, and the measurements T_L, T_C, step (°4); b) The distributions of $T_{avg}(x)$, $T_w(x)$, and the relative error of the T_w calculation procedure in [%], step (°5).

The error appears only at the end of the analysed section, when all the relevant variables and the temperature difference are already very low, and can be attributed to the inaccuracies of the interpolation in that area. The calculation of T_w(x) shown was carried assuming $\epsilon_g \approx \alpha_g = \epsilon_{12}=0.8$, see [3], [7]. If a very sooty smoke-mixture is assumed and the value increased to $\epsilon_{12} \approx 1.0$, the results do not change much.

The results obtained for the distribution of heat transfer coefficients: h_c, h_r, h_{cr}, h_{crc}, with respect to *x* are given in Fig.5. Obviosly, local h_{cr} is equal to local sum h_c+h_r. As it can be seen from the previous two figures, at approx *~300 m* the h_{cr} and h_{crc} colapse into an equal value, since from that location, the average wall-temperature $T_w(x)$ returns back to the undisturbed wall (rock-masive) temperature T_0, and both coefficients operate with the same temperature *difference*. The mean values over the analysed relevant length are given in Table 4. Analogous analysis was carried for the *120*MW test-data, the results are given in Fig.5.b and in Table 4.

For the *70*MW case, the convective, and the radiative heat transfer rate (not including the local flame-to-wall loss *(1-*η_r*)HRR* at fire-site), over the analysed length are: $\dot{Q}_r=1.496\cdot10^7$ W, $\dot{Q}_c=2.792\cdot10^7$ W. Thus, the share of the radiative part is approx. *35*%, and can not be neglected. For the *120*MW case, the same results are: $\dot{Q}_r=2.83\cdot10^7$ W, $\dot{Q}_c=4.02\cdot10^7$ W, and the radiative part share is *41.3*%.



a) b)

Fig.5. Computed heat transfer coefficients: a) *70*MW test-data; b) *120*MW test-data

Table 4: Mean values of the heat transfer coefficients for the *70*MW and *120*MW tunnel-fire test-data.

HRR	$h_{c,m}$	$h_{r,m}$	$h_{cr,m}$	$h_{crc,m}$
70 MW	*22.14*	*11.81*	*34*	*28.95*
120 MW	*25.54*	*17.88*	*43.6*	*28.24*

Depending on the calculation approach to estimate the T_{avg}(x) as a best-fit approximate solution [1],[2],[5], the designer may be interested in either $h_{cr,m}$ and T_w , [1], or in $h_{crc,m}$ only, [2], when in order to determine $T_{avg}(x)$ and Δp_{ch} eqs.(3,4,5) are used.

When compared to the generic-tunnel numerical example (HRR_{max}=*50*MW fire) the values obtained from the data processing of the *70*MW test-fire data are higher, which can be attributed to: (i) higher *HRR's (70MW vs 50MW)* effect on Re and h_c, (ii) a higher (above the limit) roughness effect on higher h_c, (iii) higher radiative share, (iv) *HRR*'s time-evolvement in test [5] vs simulated case. Having in mind $h_{c,m}$*'s HRR*-trend (i) and friction-trend of Fig.2 (ii), the $h_{r,m}$*'s HRR*-trend (iii), one could extrapolate back for an estimate value at the generic-tunnel example inputs (*50*MW, $f_D=0.0275$): $h_{c,m}\sim 16$, $h_{rm}\sim 9.3$, $h_{cr,m}\sim 25.3$, $h_{crc.m}\sim 22.5$ W/m^2K.

3. SUMMARY AND CONCLUSION

For most commonly encountered tunnels with cast-concrete walls ($D_h\approx 8m$, $u_{cr}\approx 3m/s$, $f_D\approx 0.0275$), longitudinally ventilated, at a time *15-20* min from the fire-onset, with standardized *50*MW fire, the following numerically-computed $h_{cr,m}$ *and* $h_{crc,m}$ values can be expected: *21.3, 16.1* W/m^{2}K, respectively. They can be used to asses $T_{avg}(x)$ and the Δp_{ch} in ventilation design.

For larger fires, and/or tunnels with a higher wall-roughness, higher values of $h_{c,m}$, $h_{cr,m}$ or $h_{crc,m}$ than the given numerical example will occur. Care must be exercised in evaluating them in each design case, given the complex influence of wall-roughness, radiation, flow-velocity, fire size, and a numerical approach is recommended.

Test-based values given in Table 4. can be considered as good upper-value test-based estimates given the limiting-effect of surface-roughness value in these tests, on maximizing the convective part of the overall heat transfer, at a given *HRR* value.

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