

Determination of the sensitivity behavior of an acoustic, thermal flow sensor by electronic characterization

J.W. van Honschoten^{a,*}, V.B. Svetovoy^b, T.S.J. Lammerink^a,
G.J.M. Krijnen^a, M.C. Elwenspoek^a

^a *Transducers Science and Technology Group, University of Twente, EWI, P.O. Box 217, 7500 AE Enschede, The Netherlands*

^b *Institute of Microelectronics, RAS, Yaroslavl, Russia*

Received 7 October 2003; received in revised form 7 October 2003; accepted 14 October 2003

Abstract

The microflow sensor is an acoustic, thermal flow sensor that measures sound particle velocity instead of sound pressure. It is a specific example of a wide range of two- and three-wire thermal flow sensors. For most applications the microflow sensor should be calibrated, which is usually performed acoustically in a standing-wave-tube. Here, it is shown that the sensor's sensitivity and frequency behavior can be determined electronically as well, and an electronic method for determination of the device output response, which is more convenient, is therefore presented. The method is not only less complicated, but also makes it possible to cover easily the entire acoustic frequency spectral range. The method is shown to be geometry-independent, and it can be applied for a wide range of thermal flow sensors, even for those consisting of more than two wires. The method is based on a general relation following from the heat transfer theory. This relation does not depend on the precise geometry of the sensor. The theory has been experimentally verified for various thermal flow sensors of different geometries, up to approximately 10 kHz, and a good correspondence between measurements and theory was found.

© 2003 Elsevier B.V. All rights reserved.

Keywords: Acoustic sensors; Thermal flow sensors; MEMS; Electronic characterization; Calibration

1. Introduction

The microflow sensor consists of two closely spaced silicon nitride beams ($1500\ \mu\text{m} \times 2.5\ \mu\text{m} \times 0.4\ \mu\text{m}$) with an electrically conducting platinum wiring on top (see Fig. 1). These wires act as temperature sensors and heaters. The wires are electrically powered and heated to about 600 K. When a particle velocity or flow is present, the temperature distribution around the resistors is asymmetrically altered due to convection, and a temperature difference between the two wires occurs. Because of the temperature dependence of the resistance of the wires, their resistance difference thus quantifies the local flow or particle velocity. The thermal acoustic sensor thus measures sound particle velocity instead of the usually measured sound pressure [1–5].

The sensor is a specific example of a two-wire thermal flow sensor, particularly designed and optimized for acoustic measurements. In general, micromachined flow sen-

sors can be based on different measuring principles, like thermopiles [10], pyroelectric elements [11], pn-junctions [12], and several other effects, but the main principle is that of two or more heated wires in combination with the measurement of differential temperatures [8,9]. The MEMS implementation based on silicon micromachining allows for small wire separations, small heat capacities and consequently for high sensitivity and fast responses as well as low power consumption.

In this paper an electronic method, instead of the usual acoustic method, for the determination of the device sensitivity is described. Usually, the microflow sensor is calibrated acoustically in a standing-wave-tube [1,3]. From physical principles and similarities between the governing equations [4,6], it can be proven that the acoustic behavior can be deduced from electronic measurements only. Since the method is geometry-independent, it can be applied very generally. This means that for all types of thermal flow sensors consisting of more than one wire and based on the principle of temperature gradient measurement [8,9,13,14], the electronic method can be used to deduce

* Corresponding author. Tel.: +31-53-4894438; fax: +31-53-4893343.
E-mail address: j.w.vanhonschoten@el.utwente.nl (J.W. van Honschoten).

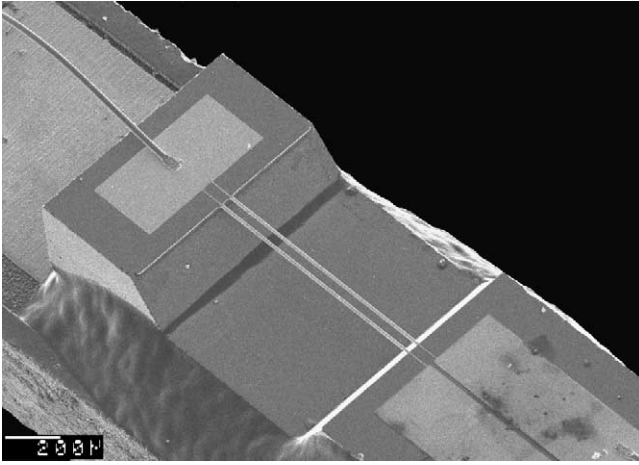


Fig. 1. SEM photo of a bridge type microflow.

the response of the sensor to fluid flows, for both ac and dc flows.

2. Theory

In this paper, it will be shown that the sensitivity of the microflow to acoustic signals can be found by using electrical measurements only. The relationship between its sensitivity and the impedances of the two wires, exists for very general assumptions about the system. The first assumption is that the heat transfer in the device can be described by the linear heat equation. Since the heat conductivity of air depends on temperature, this assumption restricts the power dissipated in the wires, as the wire temperature should stay [4] below approximately 500 K. The second assumption is that the wire width and thickness are much smaller than all the other geometrical parameters characterizing the microflow. This is typically true for all microflows used in applications, and many flow sensors.

Devices of different design are in practical use. The channel can be closed, half-open (Fig. 1), or open (free-standing wires). One can find an analytical solution for the temperature distribution in a rectangular channel [4] or in free-standing wires [5]. In the general case of arbitrary channel cross-section, one can find the solution of the heat equation in terms of unknown eigenvalues and eigenfunctions describing the temperature distribution in the channel cross-section.

The statement to be proven here is that the relation between the acoustic sensitivity and the impedances of the wires does not depend on the unknown eigenvalues and eigenfunctions and so, it has a very general character. It will be proven that the background temperature, the correction to it due to time-dependent particle velocity (sound wave) and that due to a time varying power in a wire, all can be expressed via the same Green's function. This property is true if the wires can be considered as thin. The reason is

that all these values are the solution of the same heat equation with the same boundary conditions. In this way, one can deduce a relationship between the acoustic sensitivity and the wires' electrical impedances as a function of the wires' positions. Such a relation is not very helpful in practical sense but the specific symmetry of the heat equation allows one to relate the derivative of the Green's function to sensor position with the integral over frequency from this function. In this way, the acoustic sensitivity at frequency f is connected with the electrical transfer function of the device averaged over the frequency band from 0 to a given frequency f .

The microflow theory has been developed in detail in previous work [4]. The problem was solved there for a rectangular channel but the proposed method is still valid for more general geometries. Here, we will follow the description given in that paper [4] and adopt the same notations.

2.1. Stationary temperature distribution

Let us consider first the stationary temperature distribution in the channel of the microflow (Fig. 1) when the gas inside does not move. The coordinate system is chosen as shown in Fig. 2 with the x -axis along the channel.

The wires, of length l_y , are directed along the y -direction. The distance between the wires is $2a$; one of them is located at $x = a$ and the other one at $x = -a$. If both of them are heated with a constant power P and there is no gas flow, then the temperature distribution $T(x, y, z)$ is found from the stationary heat equation:

$$-k\nabla^2 T = \frac{P}{l_y} [\delta(x - a) + \delta(x + a)] \delta(z), \quad (1)$$

where $\delta(x)$ is the Dirac delta function.

According to our assumptions in (1) the heat conductivity k was supposed not to depend on temperature and because the wires are thin and narrow the heat sources on the RHS can be described by δ -functions. Note that the power is distributed homogeneously along y and the temperature is determined by the power per unit wire length.

It is convenient to introduce the dimensionless coordinates and parameters:

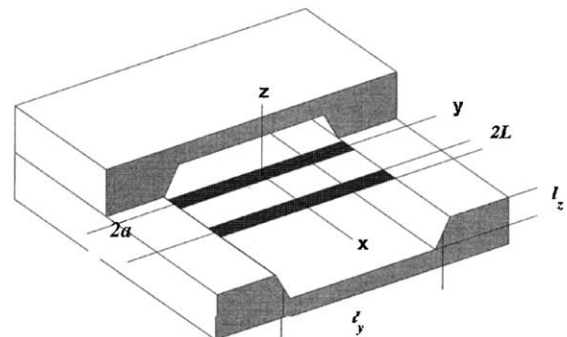


Fig. 2. Geometry of the sensor used in the analysis.

$$\xi = \frac{x}{l}, \quad \eta = \frac{y}{l}, \quad \zeta = \frac{z}{l}, \quad \xi_1 = \frac{a}{l}, \quad \xi_0 = \frac{L}{l}, \quad (2)$$

where l is any characteristic size of the channel cross-section and L the half of width of the wire. In the dimensionless coordinates, the Eq. (1) will get the form:

$$(\partial_\xi^2 + \nabla_\perp^2)T = -\frac{P}{kl_y}[\delta(\xi - \xi_1) + \delta(\xi + \xi_1)]\delta(\zeta), \quad (3)$$

where $\nabla_\perp^2 = \partial_\eta^2 + \partial_\zeta^2$ is the transverse Laplace operator.

At the channel walls, the temperature obeys homogeneous boundary conditions, for example, $T = T_0$. Because the device walls are made of silicon, which has a heat conductivity k_{Si} much larger than the air conductivity k , we can take the walls to be at the environment temperature T_r (room temperature). Even in the situation that the wire is in contact with the silicon substrate it was proven [4] that only a small part of the heat flux escapes via this contact and that the main heat flux goes via the air. Therefore, even in the contact points the same boundary conditions hold. The situation that there are no walls in any direction can be considered as a wall at infinity where the same condition ($T = T_0$) is true. Eq. (3) is linear and one can redefine the temperature $T \rightarrow T - T_0$ in such a way that the boundary condition is reduced to $T = 0$ at the channel walls.

One can define the set of functions $\psi_n(\eta, \zeta)$ which obey the boundary conditions and are the eigenfunctions of the transverse Laplace operator:

$$\nabla_\perp^2 \psi_n(\eta, \zeta) = -\lambda_n^2 \psi_n(\eta, \zeta), \quad (4)$$

with corresponding eigenvalues λ_n^2 . Here, the index n is actually a multi-index comprising two numbers (n, m) since Eq. (4) is two-dimensional. These numbers do not need to be integer; one or both of these can be continuous if the problem described by Eq. (4) has a continuous spectrum. There is no necessity to have explicit expressions for the functions $\psi_n(\eta, \zeta)$ and the values λ_n^2 , it is quite sufficient to know that the functions can be chosen orthogonal:

$$\int \psi_n \psi_m d\eta d\zeta = \delta_{nm}. \quad (5)$$

Here, δ_{nm} is the product of Kronecker symbols if both components of the multi-index are integer; one or both of the symbols have to be changed by the δ -function if one or both of these components are continuous.

The solution of Eq. (3) can be found by expanding the temperature on these functions ψ_n :

$$T(\xi, \eta, \zeta) = \sum_n T_n(\xi) \psi_n(\eta, \zeta). \quad (6)$$

If one or both components of the multi-index are continuous then the corresponding summation has to be changed by an integral. Using then the orthogonality condition (5) one can find the equation for the components $T_n(\xi)$:

$$\partial_\xi^2 T_n - \lambda_n^2 T_n = -\frac{P}{kl_y} A_n [\delta(\xi - \xi_1) + \delta(\xi + \xi_1)], \quad (7)$$

where the constants A_n are $A_n = \int \psi_n(\eta, 0) d\eta$.

The solution of Eq. (7) should obey the boundary conditions at both ends of the channel $T_n \rightarrow 0$ when $\xi \rightarrow \pm\infty$. This solution can be easily found as:

$$T_n(\xi) = \frac{P}{kl_y} \frac{A_n}{2\lambda_n} [\exp(-\lambda_n |\xi - \xi_1|) + \exp(-\lambda_n |\xi + \xi_1|)]. \quad (8)$$

One can substitute it into Eq. (6) to get the final result for the temperature distribution. Note that actually we have not solved the heat equation because the functions ψ_n and the values of λ_n have not been specified. However, Eq. (8) is everything that is needed to connect the acoustical sensitivity of the sensor with the electrical measurements.

One important detail should now be mentioned. If one would like to find the heater temperature, for example at $\xi = \xi_1$, one fails because the sum in (6) will diverge. This is because at large n , the eigenvalues λ_n are proportional to n but the coefficients A_n can be n -independent. Then, the sum in (6) will be logarithmically diverging. It is quite clear why this divergence appears. It results from the approximation that the wire can be considered as infinitely thin. There is an easy way to avoid this problem without any significant complication of the mathematics. One should average the temperature over the heater width in the place where it is located. For example, if one intends to calculate the heater temperature at $\xi = \xi_1$ one can average only the first term in (8) in the range $\xi_1 - \xi_0 < \xi < \xi_1 + \xi_0$ because the second term does not bring any trouble and is safely converging due to the presence of the exponent. The averaging then gives us:

$$\bar{T}_n(\xi_1) = \frac{P}{kl_y} A_n \left[\frac{1 - \exp(-\lambda_n \xi_0)}{2\lambda_n^2 \xi_0} + \frac{\exp(-\lambda_n |\xi + \xi_1|)}{2\lambda_n} \right].$$

Comparing it with (8) at $\xi = \xi_1$, a simple rule to avoid the divergence problem can be deduced. In the place where the divergence is possible, the following substitution has to be made:

$$\frac{1}{\lambda_n} \rightarrow \frac{1 - \exp(-\lambda_n \xi_0)}{\lambda_n^2 \xi_0}. \quad (9)$$

This change allows taking into account the final width of the wire and makes the sum in (6) convergent. Indeed, at $\lambda_n \xi_0 \ll 1$ the RHS of (9) coincides with the LHS, but at large n it behaves as $1/\lambda_n^2$ providing the sum in (6) to be convergent.

2.2. Analogy between acoustically and electrically induced disturbance

Now one can consider the situation that the gas in the channel is flowing along the channel with some velocity $v(t)$ defined by a sound wave. This movement breaks the symmetry in the temperature distribution due to the convection process. The sound velocity is typically small in comparison with the heat-diffusion velocity $D/l \sim 0.1$ m/s, where

$D = k/\rho c_p \approx 1.9 \times 10^{-5} \text{ m}^2/\text{s}$ is the heat-diffusion coefficient for air. For this reason, the convection introduces only a small correction δT^a to the temperature distribution. This correction can be found from the non-stationary heat equation when the convective term is considered as a perturbation [4]:

$$\partial_t \delta T^a - D \nabla^2 \delta T^a = -v(t) \partial_x T. \quad (10)$$

For the electrical characterization of the device, suppose that both wires are heated by a constant power P , but that one of the heaters, for example at $x = a$, is powered additionally by a small ac component $\delta P(t) \ll P$. One is interested in the correction to the sensor's temperature δT^e due to this additional ac power. It can be found from the following equation:

$$\partial_t \delta T^e - D \nabla^2 \delta T^e = \frac{\delta P(t)}{l_y} \frac{1}{\rho c_p} \delta(x - a) \delta(z). \quad (11)$$

Of course, the Eqs. (10) and (11) are the same but the sources on the RHS, which define the solutions, are quite different. In the acoustic case, a source is distributed along the channel axis, but in the electric case the source is located on the wire. When the sources are different, the solutions will also differ. However, because the unperturbed temperature T obeys the heat equation for both situations, in the limit of small perturbations, it is possible to connect δT^a and δT^e .

Consider the case of a harmonically in time-varying sound wave and an ac power:

$$v(t) = v \exp(i\omega t), \quad \delta P(t) = \delta P \exp(i\omega t), \quad (12)$$

with the corresponding frequency $f = \omega/2\pi$. Introducing the dimensionless coordinates and frequency:

$$\bar{f} = \frac{\omega l^2}{D}, \quad (13)$$

one gets instead of (10) and (11):

$$\begin{aligned} \nabla^2 \delta T^a - i \bar{f} \delta T^a &= -\frac{v}{v_0} \partial_\xi T, \\ \nabla^2 \delta T^e - i \bar{f} \delta T^e &= \frac{\delta P}{kl_y} \delta(\xi - \xi_1) \delta(\zeta), \end{aligned}$$

where $v_0 = D/l$ represents the diffusion velocity. The temperature correction obeys the same boundary conditions as the temperature itself: it disappears on the channel walls and is going to zero at $\xi \rightarrow \pm\infty$. Therefore, we can expand δT (acoustic or electric) in the same eigenfunctions ψ_n :

$$\delta T(\xi, \eta, \zeta) = \sum_n \delta T_n(\xi) \psi_n(\eta, \zeta). \quad (14)$$

Substituting it into the equations above one finds:

$$\begin{aligned} \partial_\xi^2 \delta T_n^a - K_n^2 \delta T_n^a &= \frac{v}{v_0} A_n \partial_\xi T_n, \\ \partial_\xi^2 \delta T_n^e - K_n^2 \delta T_n^e &= -\frac{\delta P}{kl_y} A_n \delta(\xi - \xi_1), \end{aligned} \quad (15)$$

where $K_n^2 = \lambda_n^2 + i \bar{f}$.

To see the correspondence between δT^a and δT^e , it will be convenient to write the solutions via the same Green's function $G_n(f, \xi - \xi')$ which, by definition, obeys the equation:

$$\partial_\xi^2 G_n(f, \xi - \xi') - K_n^2 G_n(f, \xi - \xi') = \delta(\xi - \xi'). \quad (16)$$

It has a well known solution:

$$G_n(f, \xi - \xi') = -\frac{1}{2K_n} \exp(-K_n |\xi - \xi'|). \quad (17)$$

Using this function the solution of the Eq. (15) can be written then as follows:

$$\begin{aligned} \delta T_n^a &= \frac{v}{v_0} A_n \int_{-\infty}^{\infty} G_n(f, \xi - \xi') \partial_\xi T_n(\xi') d\xi', \\ \delta T_n^e &= -\frac{\delta P}{kl_y} A_n G_n(f, \xi - \xi_1). \end{aligned} \quad (18)$$

The unperturbed temperature distribution $T_n(\xi)$ given by (8) can also be represented via the same Green's function but then taken at zero frequency:

$$T_n(\xi) = -\frac{P}{kl_y} A_n [G_n(0, \xi - \xi_1) + G_n(0, \xi + \xi_1)]. \quad (19)$$

Using only general properties of the Green's function, one can transform the integral in (18) to the form:

$$\begin{aligned} i \bar{f} \delta T_n^a &= \frac{v}{v_0} \frac{P}{kl_y} A_n \partial_\xi \{G_n(0, \xi - \xi_1) + G_n(0, \xi + \xi_1) \\ &\quad - G_n(f, \xi - \xi_1) - G_n(f, \xi + \xi_1)\}. \end{aligned} \quad (20)$$

Now it is seen that the acoustic and electric corrections to the temperature are really related to each other. However, there is no direct proportionality between δT^a and δT^e . Moreover, δT^a depends on the derivative on ξ , which we are not able to control. The actual precise relation comes from a specific property of the Green's function for the heat equation:

$$\begin{aligned} \partial_\xi [G_n(f, \xi - \xi') - G_n(0, \xi - \xi')] \\ = \frac{i}{2} (\xi - \xi') \int_0^{\bar{f}} G_n(f, \xi - \xi') d\bar{f}, \end{aligned} \quad (21)$$

which can be checked directly with the help of (17). In this way, a general relation can be deduced, connecting the temperature response of the microflow to an acoustic wave with the Green's function, which, on its turn, is proportional to the temperature response to the electric signal:

$$\begin{aligned} \delta T_n^a &= -\frac{v}{2v_0} \frac{P}{kl_y} A_n \frac{1}{f} \int_0^{\bar{f}} [(\xi - \xi_1) G_n(f, \xi - \xi_1) \\ &\quad + (\xi + \xi_1) G_n(f, \xi + \xi_1)] d\bar{f}. \end{aligned} \quad (22)$$

This relation is true for any point along the channel.

For practical purposes, one is interested in the temperatures of the sensors that are located at $z = 0$ and $x = a$ or $x = -a$. Additionally, since the sensor resistances are really important, the temperatures have to be averaged over the

wire length. Now these averaged temperatures are denoted as ΔT_1 and ΔT_2 for the sensors located at $x = a$ and $x = -a$, respectively. Using (14), (18), and (22) for the mean wire temperatures characterized electrically or acoustically, one finds:

$$\begin{aligned} \Delta T_1^e &= -\frac{\delta P}{kl_y} \sum_n \frac{l}{l_y} A_n^2 G_n(f, 0), \\ \Delta T_2^e &= -\frac{\delta P}{kl_y} \sum_n \frac{l}{l_y} A_n^2 G_n(f, 2\xi_1), \\ \Delta T_1^a &= -\Delta T_2^a = -\frac{v}{v_0} \frac{P}{kl_y} \frac{\xi_1}{f} \int_0^f \sum_n \frac{l}{l_y} A_n^2 G_n(f, 2\xi_1) df. \end{aligned} \quad (23)$$

Therefore, the final relation between the averaged sensor temperatures in the acoustical and electrical characterization becomes obvious:

$$\Delta T_1^a = \frac{v}{v_0} \frac{P}{\delta P} \frac{\xi_1}{f} \int_0^f \Delta T_2^e df \quad (24)$$

It shows that the acoustical response at a frequency f is proportional to the electrical response averaged over the frequency range from 0 to f . This is a non-trivial and unobvious relation that will be true for any device as long as the wires are thin and the temperature dependence of the heat conductivity can be neglected (linear heat equation). For thin wires, it was natural to suppose that their heat capacity is not important, but in reality it can be relevant at high frequencies. The method to take this finite heat capacity into account has been proposed in previous work [4] and can also be applied to the problem of electrical characterization of the device. However, it is out of the scope of this paper and will be discussed elsewhere.

2.3. Implications for the electrical signals

In practical applications the voltage of a wire is often easily measured, and in the electrical characterization one measures the voltage of the wire(s) as a function of frequency. For this reason, the relation (24) has to be expressed via directly measured values. The voltage is connected with the current I flowing via the sensor by Ohm's law:

$$U = IR_0[1 + \alpha T(P)], \quad (25)$$

where R_0 is the sensor resistance at room temperature, α the temperature coefficient, and $T(P)$ the sensor temperature above the room temperature at a given power $P = UI$. When the device is operating as an acoustic sensor, a stabilized voltage source U_0 is used and the signal u^a is recorded as indicated in Fig. 3.

In this case, the temperature change of the sensor is related to the external influence: the acoustic wave. If the wave has a frequency f then the signal on this frequency can be written

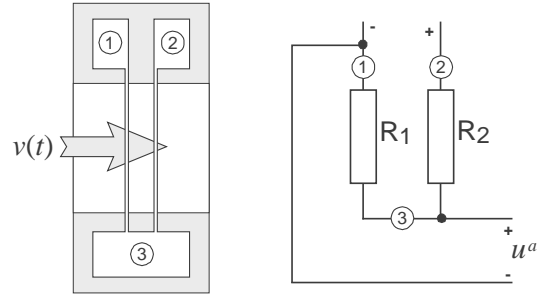


Fig. 3. Electrical scheme of the microflow, operating acoustically; $R_1 = R_2 = R$; u^a is the output signal.

as:

$$u^a = \frac{U_0}{2} \alpha \frac{R_0}{R} \Delta T_1^a, \quad (26)$$

where ΔT_1^a is defined at operating power $P = U_0^2/R$ and R the sensor resistance at operating temperature.

In the case of the electrical characterization, a stabilized dc current I_0 is flowing through each wire, while an additional ac component flows through wire 1 ($x = a$). The resulting current for the wire 1 can be written as $I = I_0 + \delta I \cos(\omega t)$. The amplitude of the ac component δI can always be chosen small not to complicate the analysis. Neglecting then the higher order terms of ac components, the additional oscillating power in the wire can be represented as:

$$\delta P = u_1^e I_0 + U_0 \delta I, \quad (27)$$

where $U_0 = U(I_0)$ is the dc component of the voltage. The ac voltage measured on wire 1 can be written then using the Ohm law:

$$u_1^e = R \delta I + \alpha R_0 I_0 \Delta T_1^e.$$

Since the sensor temperature is proportional to δP , we can express the ac voltage on the wire 1 as:

$$u_1^e = \delta IR \frac{1 + S_1}{1 - S_1}, \quad S_1 = -\alpha \frac{R_0}{R} \frac{P_0}{kl_y} \sum_n \frac{l}{l_y} A_n^2 G_n(f, 0). \quad (28)$$

Here, P_0 represents the dc power. The wire 2 is powered with only a dc current and the ac voltage on this wire will be:

$$u_2^e = \alpha I_0 R_0 \Delta T_2^e.$$

The temperature correction is again proportional to the amplitude of the oscillating δP , which can be found from (27). Thus, one can find:

$$u_2^e = \delta IR \frac{2S_2}{1 - S_1}, \quad S_2 = -\alpha \frac{R_0}{R} \frac{P_0}{kl_y} \sum_n \frac{l}{l_y} A_n^2 G_n(f, 2\xi_1). \quad (29)$$

Using the basic relation (24), one can express now the acoustic signal (26) via the electric signals (28) and (29):

$$u^a = \left(\frac{U_0}{2} \right) \frac{va}{D} \frac{1}{f} \int_0^f \frac{u_2^e}{\delta IR + u_1^e} df. \quad (30)$$

This is the final relation one has been looking for. It contains the only geometrical parameter of the device, the mutual wire distance a , and only one medium parameter, the fluid characteristic D . All the other parameters are electrical, and well defined.

The electrical characterizations of the device gives the values, $u_{1,2}^e$, as functions of frequency. To get the acoustic signal, the integral in (30) should be calculated numerically. To do this one needs to extrapolate the integrand to smallest frequencies, which are not accessible in the measurements. The low frequency behavior of $u_{1,2}^e$ is easy to investigate analyzing the Green's function (17) in the limit $f \rightarrow 0$. This analysis gives:

$$F(f) = \frac{u_2^e}{\delta I R + u_1^e} \rightarrow (A + iBf) \quad \text{at } f \rightarrow 0,$$

where A and B are some constants. If the experimental cut-off frequency is f_0 , then:

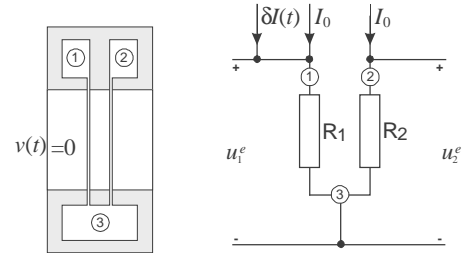


Fig. 4. Scheme of the set-up used in the electrical characterization. Now $v(t) = 0$ (compare with Fig. 3) and the additional current is $\delta I(t)$.

$$\int_0^{f_0} F(f) df = f_0 \left[\text{Re } F(f_0) + \frac{i}{2} \text{Im } F(f_0) \right]. \quad (31)$$

This relation defines the extrapolation procedure and so the problem can be considered as completely solved.

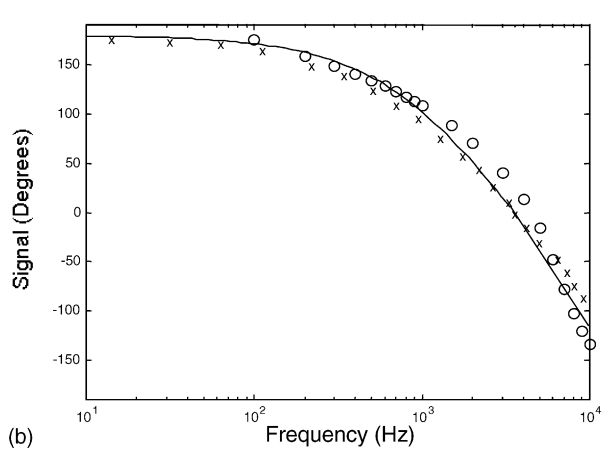
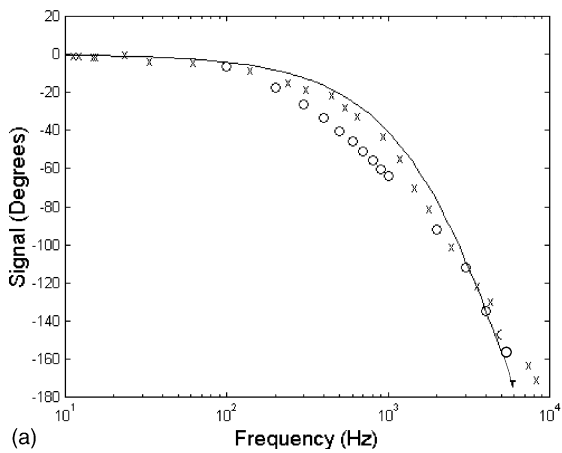
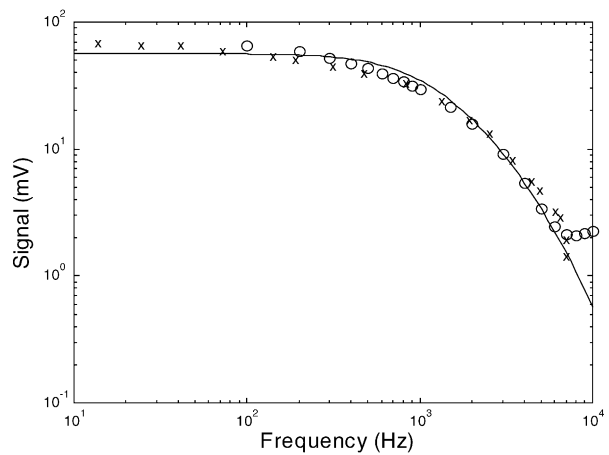
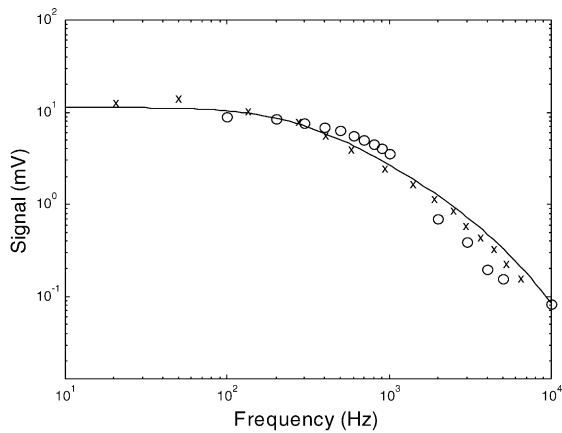


Fig. 5. (a) Electrically determined points (crosses) of the sensitivity and the acoustically measured points (circles), together with the theoretical prediction for the acoustic sensitivity of the sensor, using model calculations [4,5] (line), for 'device 1': etch depth $l_z = 40 \mu\text{m}$; mutual wire distance $2a = 100 \mu\text{m}$; wire resistances $R_1 = 3274 \Omega$; $R_2 = 3620 \Omega$. Both the amplitude and the phase of the response are shown. (b) As (a), for 'device 2': etch depth $l_z = 240 \mu\text{m}$; mutual wire distance $2a = 50 \mu\text{m}$; wire resistances $R_1 = 3425 \Omega$; $R_2 = 3464 \Omega$. (c) As (a), for 'device 3': etch depth $l_z = 240 \mu\text{m}$; mutual wire distance $2a = 100 \mu\text{m}$; wire resistances $R_1 = 2920 \Omega$; $R_2 = 2875 \Omega$. (d) As (a), for 'device 4': etch depth $l_z = 240 \mu\text{m}$; mutual wire distance $2a = 300 \mu\text{m}$; wire resistances $R_1 = 1411 \Omega$; $R_2 = 1414 \Omega$.

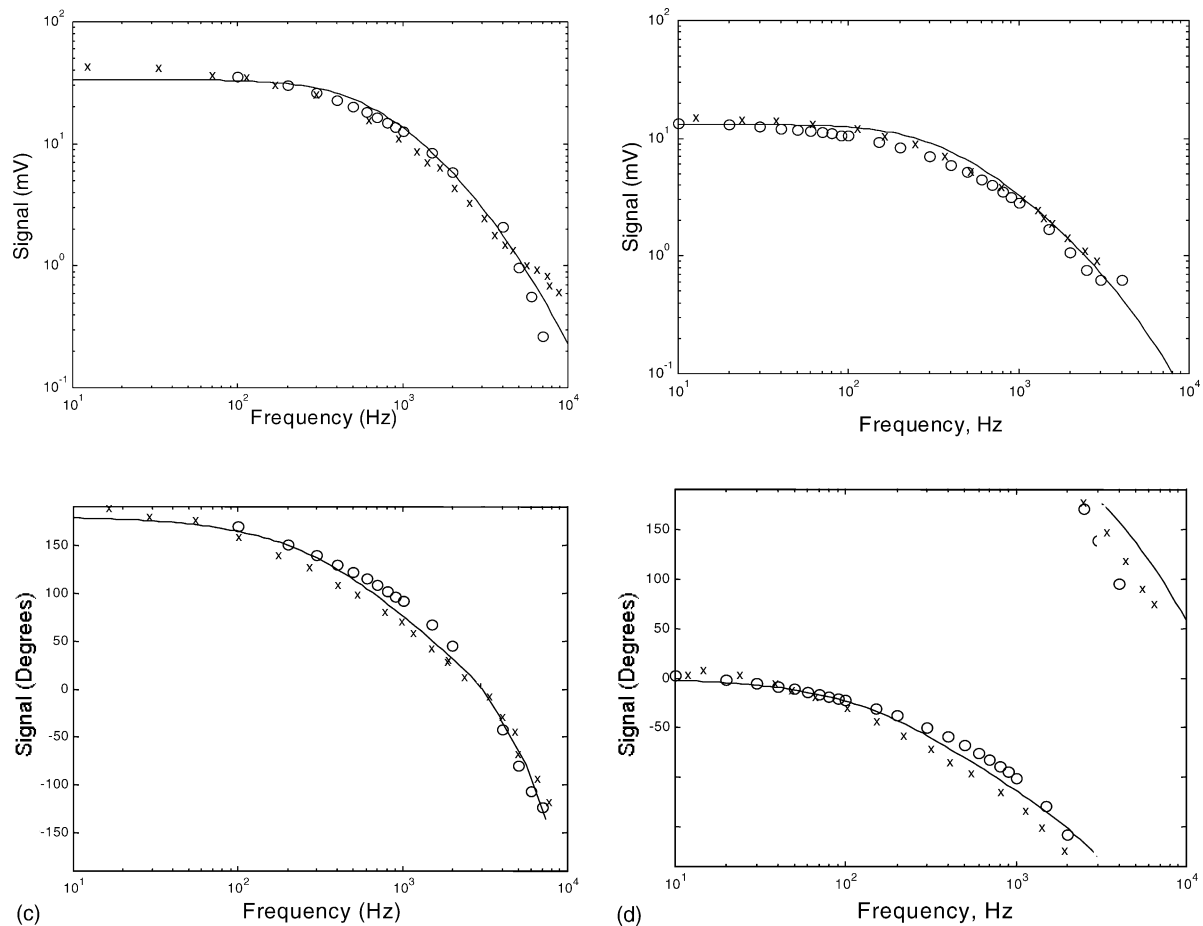


Fig. 5. (Continued).

A different geometry of the acoustic flow sensor can be realized with three instead of two wires. In this configuration, the central wire acts as heater, while the other two wires, in which a relatively low power is dissipated, act as sensing wires [7]. This sensor-heater-sensor (SHS) configuration leads to an improvement of the performance of the sensor; it has been shown [7] that in the three-wire sensor an approximately two times higher low-frequency sensitivity than in a two-wire situation can be attained, if in the central heated wire approximately 85% of the total power is dissipated. Since most of the power is dissipated in the central heater, which does not act as a sensor, the two sensing wires have a relatively low temperature and therefore a relatively low noise level. Following a similar approach as above, Eqs. (15)–(24), it can be deduced that, in this three-wire situation, the acoustical response of one wire can again, similarly, be expressed in the electrical responses of this wire to the additional powering of the other two wires. Both of the other two wires have to be powered apart, independently. For the three-wire thermal flow sensors, with arbitrary powers in the different wires, this means that principally the problem is also solved.

3. Experiments

In the experimental set-up both wires of the (two-wire) sensor were connected as shown in Fig. 4. The two wires were powered using a stabilized dc current I_0 . Using the current source, the frequency of the additional current δI was varied in a broad frequency range while both the voltage of wires A (resistance R_1) and B (resistance R_2) were recorded, using a lock-in amplifier. Using Eq. (30), the predicted acoustic sensitivity as a function of frequency was determined. The low experimental cut-off frequency was taken as 10 Hz. From this numerical procedure, the predicted acoustic response of the sensor was found to be as the graphs plotted in Fig. 5.

The dissipated dc power in each wire was set to be 10 mW, by adjusting the constant current I_0 through wires A and B. The additional oscillating current δI through wire A was chosen to be $\delta I = 0.01 I_0$. The electronic responses of wires A and B were recorded using a lock-in amplifier. Both amplitude and phase were recorded in a frequency range 10 Hz to 10 kHz.

Besides, the sensitivity was determined acoustically, i.e., in a ‘standing-wave-tube’ [1–3], a tube of about 1 m length

and approximately 10 cm diameter with at one side, a loudspeaker generating a broad frequency band signal, and at the other side a reference microphone. From the ratio between the output signals of the microphone and the microflow, the sensitivity of the latter could be deduced.

The described set-up and experimental approach was used for several sensors. As is known from theory [4,5], the dimensions of the sensor, in particular the mutual wire distance and the etch depth (the distance to the channel surface, l_z), determine the precise frequency behavior of the sensitivity function. To investigate these effects and to verify the geometry-independence of the correspondence between electronic and acoustic behavior, four microflows of different geometry were analyzed. The sensors had a wire length l_y of 1 mm, and the mutual distance $2a$ between the wires varied from 50 and 100 to 300 μm . The etch depth of the channel, i.e., the vertical distance l_z to the heat sink, varied between 40 and 240 μm .

Fig. 5 shows the electronically obtained results (the crosses), together with the acoustic sensitivity of the sensor as measured in the standing-wave-tube (circles). Additionally, the acoustic sensitivity is theoretically determined from the model that calculates the sensor output for a given sensor geometry [4,5]. Experiment and theory were compared for these four different geometries.

From these figures, it can be concluded that there is a satisfying correspondence between the electrically determined measurement points of the sensor sensitivity and the acoustically determined sensitivity (determined in the standing-wave-tube). Besides they can be well related to the theoretical prediction for the output signal for the different geometries. Nevertheless, small deviations are observed for high frequencies. These deviations may be explained by the finite heat capacities of the sensor wires, that have not been taken into account in the described approach.

4. Conclusions

An electrical characterization method for the sensitivity of the microflow was presented. In conclusion, it is shown that the new calibration method is a more convenient and less complicated method than the acoustic calibration of the microflow using, e.g., a standing-wave-tube. It is proven from physical principles and correspondences in physical equations that this method yields all the required information to deduce the sensor's acoustic response from electronically obtained information. The strength of the theoretical description and prediction of the acoustic response lies in the fact that it is very general and independent of the precise geometry of the sensor. It can be shown that for a three-wire configuration the method can be applied as well. This means that both two- and three-wire thermal flow sensors can be electrically characterized. The theory has been experimentally verified for various geometrically

different two-wire microflow up to approximately 10 kHz, and a good correspondence between measurements and theory was found.

Acknowledgements

The authors would like to thank Phillip Ekkels for his willing cooperation and support, and M. de Boer and J.W. Berenschot for their technical assistance. Thank is given to the Dutch Technology Foundation STW for its financial support.

References

- [1] H.-E. de Bree, et al., The microflow: a novel device measuring acoustical flows, *Sens. Actuators A* 54 (1996) 552–557.
- [2] F.J.M. van der Eerden, et al., Experiments with a new acoustic particle velocity sensor in an impedance tube, *Sens. Actuators A* 69 (1998) 126–133.
- [3] H.E. de Bree, et al., Realization and calibration of a novel half inch p-u sound intensity probe, in: *Proceedings of the 106th AES*, Munchen, 1999.
- [4] V.B. Svetovoy, I.A. Winter, Model of the microflow microphone, *Sens. Actuators* 86 (2000) 171–181.
- [5] J.W. van Honschoten, et al., Optimization of a two-wire thermal sensor for flow and sound measurements, in: *Proceedings of MEMS'01*, Interlagen.
- [6] Kielbasa, et al., Heat waves in flow metrology, in: H.H. Dijkstra, E.A. Spencer (Eds.), *Flow Measurement of Fluids*, Polish Academy of Sciences, 1995.
- [7] J.W. van Honschoten, G.J.M. Krijnen, P. Ekkels, M.C. Elwenspoek, Geometrical optimisation of a thermal flow sensor, in press.
- [8] H. Kuttner, G. Urban, A. Jachimowicz, F. Kohl, F. Olcaytug, P. Goiser, Microminiaturized thermistor arrays for temperature gradient, flow and perfusion measurements, *Sens. Actuators A* 25–27 (1991) 641–645.
- [9] A. Glaninger, A. Jachimowicz, F. Kohl, R. Chabicovsky, G. Urban, Wide range semiconductor flow sensors, *Sens. Actuators A* 85 (2000) 139–146.
- [10] D. Moser, R. Lenggenhager, H. Baltes, Silicon gas flow sensors using industrial CMOS and bipolar IC technology, *Sens. Actuators A* 25–27 (1991) 577–581.
- [11] H.Y. Hsieh, A. Spetz, J.N. Zemel, Pyroelectric anemometry: vector and swirl measurements, *Sens. Actuators A* 49 (1995) 141–147.
- [12] R. Kersjes, F. Liebscher, E. Spiegel, Y. Manoli, W. Mokwa, An invasive catheter flow sensor with on-chip CMOS readout electronics for the on-line determination of blood flow, *Sens. Actuators A* 54 (1996) 563–567.
- [13] K. Hirata, M. Esashi, Stainless steel-based integrated mass-flow controller for reactive and corrosive gases, *Sens. Actuators A* 3200 (2002).
- [14] T.S.J. Lammerink, N.R. Tas, M.C. Elwenspoek, J.H.J. Fluitman, Microliquid flow sensor, *Sens. Actuators A* (1993) 45–50.

Biographies

J.W. (Joost) van Honschoten was born in Deventer, The Netherlands, in 1972. He studied applied physics at the University of Twente, Enschede, The Netherlands, during which period he worked on solitary waves in

optical wave guides and on coherence effects in laser Doppler blood perfusion measurements. His PhD research at the University of Twente, that he is finishing in 2004, involves the modeling and optimization of an acoustic hot-wire flow sensor.

Miko C. Elwenspoek was born in Eutin, Germany, in 1948. He studied physics at the Free University of Berlin, Berlin, Germany, and received his PhD degree from the University of Berlin in 1983. His PhD research involved relaxation measurements on liquid metals and alloys, in particular alkali metal alloys. From 1977 to 1979, he was involved with the study of lipid double layers. In 1983, he began studying crystal growth of organic crystals at the University of Nijmegen, Nijmegen, The Netherlands. In 1987, he joined the University of Twente, Enschede, The Netherlands, to take charge of the Transducers and Science Technology Group, MESA+ Research Institute. He has been a full professor since 1996. His research interests include the fabrication techniques such as the physical chemistry of wet anisotropic etching, reactive ion etching, wafer bonding, chemical-mechanical polishing, and the materials science of various thin films.

Vitaly B. Svetovoy was born in Yaroslavl, Russia in 1955. He received the Master's degree in theoretical and mathematical physics from Yaroslavl University in 1978, and PhD in high energy physics from Belorussian University, Minsk in 1985. He joined the Institute of Microelectronics in 1985 and was head of the research group since 1989. During the past decade, he was active in silicon etching by plasma, laser direct writing in polymers, properties of porous silicon. His current research

interests are in measurements of very weak forces with the help of microtechnologies.

G.J.M. (Gijs) Krijnen received his MSc degree in electrical engineering from the University of Twente following a study on magnetic recording carried out at the Philips Research Laboratories, Eindhoven. In his PhD, he worked on non-linear integrated optics devices. From 1992–1995, he was a fellow of the Royal Netherlands Academy of Arts and Sciences and studied second- and third-order non-linear integrated optics devices. In this period, he was a visiting scientist at the Center for Research and Education in Optics and Lasers in Orlando, FL, USA. He was the 1993 recipient of the Veder price of the Dutch Electronics and Radio Engineering Society (NERG). In 1995–1997, he worked on integrated optic devices for optical telecommunication simultaneously at the University of Twente and the Delft University of Technology. Since 1998, he is a staff member of the Micro-Mechanics Group of the MESA+ Research Institute. His current interests include (modeling of) MEMS and MOEMS devices in general and microactuators in particular.

Theo S.J. Lammerink was born in Tubbergen, The Netherlands in 1956. He studied electrical engineering at the University of Twente, Enschede, The Netherlands and received his PhD degree from the same university in 1989. His PhD research involved optical operation of micromechanical transducers. From that moment on he was active in microfluidic systems research with emphasis on micro flow sensors. His current interests include (micro)instrumentation for MEMS devices.