Towards a sub 15-dBA optical micromachined microphone

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Micromachined microphones with grating-based optical-interferometric readout have been demonstrated previously. These microphones are similar in construction to bottom-inlet capacitive microelectromechanical-system (MEMS) microphones, with the exception that optoelectronic emitters and detectors are placed inside the microphone's front or back cavity. A potential advantage of optical microphones in designing for low noise level is the use of highly-perforated microphone backplates to enable low-damping and low thermal-mechanical noise levels. This work presents an experimental study of a microphone diaphragm and backplate designed for optical readout and low thermal-mechanical noise. The backplate is 1 mm × 1 mm and is fabricated in a 2- μ m-thick epitaxial silicon layer of a silicon-on-insulator wafer and contains a diffraction grating with 4- μ m pitch etched at the center. The presented system has a measured thermal-mechanical noise level equal to 22.6 dBA. Through measurement of the electrostatic frequency response and measured noise spectra, a device model for the microphone system is verified. The model is in-turn used to identify design paths towards MEMS microphones with sub 15-dBA noise floors.

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I. INTRODUCTION AND A REVIEW OF NOISE SOURCES IN MINIATURE MICROPHONES

Microelectromechanical-system (MEMS) microphones have been investigated for over 25 years. Review articles comparing research on capacitive, piezoelectric, and piezoresistive microphones were written on MEMS microphones as early as 1991.^{1,2} Piezoelectric microphones typically use thin films patterned atop passive diaphragms which operate in the “3–1” mode.^{3,4} These microphones have been used to address high dynamic-range applications including aero-acoustic measurements. The lowest noise demonstrations of silicon microphones have utilized capacitive-based detection. A rigorous analysis and experimental investigation into the performance of capacitive MEMS microphones to address scientific measurement applications is provided by Scheeper *et al.*⁵ The majority of capacitive MEMS microphones utilize parallel-plate capacitive detection, in which a rigid backplate is suspended in close proximity (2 to 3- μ m gap) to a compliant diaphragm structure.^{6–8} Two backplates may also be used, as demonstrated in a microphone addressing high dynamic-range measurement applications.^{9,10} Many different materials have been used for fabricating the backplate and diaphragm layers, including SiN, polysilicon, polyimide, and single-crystal epitaxial silicon.^{7,11–13} Capacitive MEMS microphones were first commercialized in 2003 to address high-volume consumer electronics applications,⁶ and today more than 1-billion microphones are shipped annually. Recently, there has been interest in optimizing miniature MEMS microphones for high signal-to-noise ratio (SNR).^{8,14} Unlike MEMS accelerometers and gyroscopes, MEMS microphones cannot be hermetically

sealed or sealed under vacuum. They must be exposed to the atmosphere in order to sense sound pressure. For these reasons, systematic analysis of frequency response and self-noise of MEMS microphones must include the complete microphone package.^{8,14–16} Figure 1 presents a schematic of a bottom-inlet microphone, with the lumped-parameter acoustical-mechanical network superimposed onto the device schematic. Such models are commonly used to study the microphone's dynamic response to sound pressure³ as well as the microphone's thermal-mechanical noise spectrum resulting from various dissipative elements within the package.¹⁵ The inspiration to directly show the network superimposed onto the device is from Földner *et al.*,¹⁴ and the overall structure of the model presented in Fig. 1 is identical to that summarized by Földner *et al.* A dominant noise source in MEMS microphones is thermal-mechanical noise (i.e., Brownian motion of the diaphragm) arising from dissipative mechanisms within the microphone and surrounding package.^{17,18} As summarized by Gabrielson, the response of the system to thermal-mechanical noise may be studied by placing a pressure noise generator $P = \sqrt{4k_b TR_a}$ [Pa/ $\sqrt{\text{Hz}}$] in series with each dissipative acoustical element in the network, and solving for the resulting diaphragm displacement.¹⁹ Finally, the diaphragm displacement can be referred to an equivalent input pressure to compute an input-pressure-referred thermal-mechanical noise for each individual dissipative mechanism. The total thermal-mechanical noise is the incoherent summation of each individual noise source. The dominant thermal-mechanical noise sources within the package are those arising from the vent resistance (i.e., vent noise), the inlet port-resistance (i.e., port noise), and the backplate flow-resistance (i.e., backplate-induced noise). All three thermal-mechanical pressure noise generators are included in Fig. 1, along with the acoustic signal pressure, P_{in} . The vent is required to filter

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Variable	Description
m_{ad}	Diaphragm mass in acoustic domain
C_{ad}	Diaphragm compliance in acoustic domain
m_{abp}	Backplate mass in acoustic domain
C_{abp}	Backplate compliance in acoustic domain
R_{am}	Mechanical flow resistance in acoustic domain
m_{port}	Inlet-port acoustic mass
R_{port}	Inlet-port acoustic resistance
R_{vent}	Vent acoustic resistance
C_{fc}	Front-cavity acoustic compliance
C_{bc}	Back-cavity acoustic compliance
$P_{tm,vent}$	Thermal-mechanical noise due to R_{vent}
$P_{tm,port}$	Thermal-mechanical noise due to R_{port}
$P_{tm,bp}$	Thermal-mechanical noise due to R_{am}

FIG. 1. (Color online) Schematic of a bottom-inlet optical MEMS microphone with superimposed acoustical-mechanical network model (structural damping due to material loss is not shown since it is negligible in comparison to other dissipative elements).

the DC response to ambient atmospheric pressure. A low-frequency analysis of the network reveals that the vent resistance and the back-cavity compliance determine the pole frequency, $f_c = 1/(2\pi R_{vent} C_{bc})$, at which the diaphragm displacement caused by the vent noise starts to decay at 20 dB/decade. This same corner frequency is the lower-limiting frequency of the microphone.

Modern bottom-port MEMS microphones employ back cavities as small as 4.4 mm^3 [Refs. 20 and 21]. Assuming 4.4-mm^3 volume and an acoustical vent-resistance chosen to realize a 20-Hz corner, the input-pressure-referred vent-noise spectrum is presented in Fig. 2(a). The input-pressure-referred noise is obtained by first solving for the diaphragm displacement noise density due to vent noise and dividing this displacement by the mechanical sensitivity of the microphone to refer the displacement to an equivalent input pressure. The mechanical sensitivity of the microphone is defined as the diaphragm displacement per input sound pressure. The A-weighted noise of this spectrum alone may be computed as 9.13 dBA. ²² This illustrates that, while vent noise is an important parameter to consider, it does not preclude the realization of an ultra-low noise, ultra-miniature microphone. Thermal-mechanical pressure noise arising from the acoustical inlet port-resistance appears directly at the microphone input and has a flat input-pressure-referred noise spectrum. A circular inlet with $150\text{-}\mu\text{m}$ radius on a

(a)

(b)

FIG. 2. (Color online) (a) Port and vent noise simulations; (b) the A-weighting filter function itself and applied to the combined port-and-vent noise spectrum.

200- μm -thick printed circuit board (PCB) has a computed acoustical resistance of $1.87 \times 10^7 \text{ Pa s/m}^3$, following an analytical equation for laminar flow through an inlet (note: while end-corrections are included for computation of inlet mass, no inlet correction is used in this computation of inlet resistance).^{23,24} The generated thermal-mechanical noise is $0.55 \mu\text{Pa}/\sqrt{\text{Hz}}$, and this flat spectrum has an A-weighted noise corresponding to 9.4 dBA. For completeness, the combined vent-and-port noise spectrum is also presented in Fig. 2(a), and the A-weighted noise power of the combined spectrum along with the A-weighting filter function itself is presented in Fig. 2(b). The combined vent-and-port noise yields an A-weighted noise of 12.3 dBA, or a signal-to-noise ratio (SNR) of 81.7 dB for a 1-Pa [94-dB sound pressure level (SPL)] input signal level. These computations illustrate that vent-and-port noise, while important to consider, do not preclude the realization of an ultra-low noise, ultra-miniature MEMS microphone.

Microphone backplate resistance is the biggest impediment to achieving low thermal-mechanical noise in capacitive microphones. Backplate resistance in MEMS devices and microphones in particular has been studied analytically

and experimentally.^{25–28} The design path for lowering the acoustical resistance of the backplate is the use of higher perforation and less backplate area, but this approach also reduces the active sensing capacitance of the structure and creates electronic design challenges,¹⁴ resulting in electronic noise levels that exceed thermal-mechanical noise levels. A design compromise thus exists between electrical and mechanical domains in designing small-scale capacitive microphones for high SNR.¹⁴ This has motivated the exploration of optical-readout techniques that can provide sensitive readout of diaphragm motion without relying on backplate capacitance for signal detection. A concept of an optical-based acoustic transducer was first introduced in 1880.²⁹ In a review article, Bilaniuk summarized various sensing techniques for optical microphone applications.³⁰ Several different readout techniques have been used to realize optical-based microphones using fiber optics,^{31,32} a combination of laser-emitting diodes (LED) and a charge-coupled device (CCD),³³ and optical-lever approaches.³⁴ A microphone that uses interference through a diffraction grating has also been introduced in prior work.^{23,35–41} The microphone uses a backplate to hold the grating in place beneath a vibrating diaphragm and also to enable the application of electrostatic forces to the diaphragm. Since backplate capacitance is not required for signal detection, the design of highly perforated backplates with low thermal-mechanical noise is permitted.^{23,36,37}

II. GRATING-BASED OPTICAL INTERFERENCE MICROPHONES

The construction of grating-based optical microphones is similar to that of modern bottom-inlet capacitive MEMS microphones, with the exception that the backplate contains a diffraction grating etched near the center. The grating-diaphragm system depicted schematically in Fig. 3 is illuminated from the backside with coherent light from a semiconductor laser such as a vertical-cavity surface-emitting laser (VCSEL).

The grating is optically reflective, with slots etched to allow passage of only a portion of the incident light to travel

FIG. 3. (Color online) Schematic of the grating-based optical interference microphone.

FIG. 4. (Color online) (a) Schematic of a surface-mountable optical MEMS microphone and (b) a 3-D CAD image describing the VCSEL and PD array setup (the diaphragm is transparent to show the backplate structure).

to the diaphragm and back to accrue additional phase, ϕ_d . The inset of Fig. 3 highlights the interference physics at the grating. The field returning to the plane of the optoelectronics is a superposition of the field reflecting directly off of the grating with reference phase, ϕ_r , and the phase-modulated field, ϕ_d . The result is a diffracted field comprised of a zeroth-order and $\pm$ first-order beams as labeled in Fig. 3.

The departing angles of the beams are fixed by the pitch of the grating, and the intensities of the beams are modulated by the diaphragm-grating gap height with the same sensitivity of a Michelson-type interferometer. This system has been modeled and experimentally studied in detail in several prior works, with application to force microscopy,⁴² ultrasonic sensors,⁴³ accelerometers,^{44,45} and microphones.³⁷ The system has been used to create surface-mountable optical MEMS microphones²³ as presented schematically in Fig. 4(a). Figure 4(b) presents a zoomed-in computer-aided design (CAD) image of the VCSEL and three-element photodiode (PD) array contained within the KOH-etched cavity of the MEMS die. The CAD image also highlights the incident beam and the return of the center and outer beam signals to the PD array. In this work, the fabrication of a low thermal-mechanical noise backplate design is presented, and the backplate is integrated with a microphone diaphragm to demonstrate a microphone sensing structure with the potential to achieve sub 20-dBA noise floor.

III. MICROFABRICATION OF PROTOTYPE SENSING STRUCTURE

The grating and diaphragm elements depicted schematically in Figs. 3 and 4 may be fabricated on the same silicon die, or on separate dies in a wafer bonding process. The

FIG. 5. (Color online) Cross-section of the prototype.

former is likely a more cost-effective solution, while the latter enables rapid prototyping of each component separately. In this work, a two-chip solution is used. The backplate is etched into a $2\text{-}\mu\text{m}$ -thick device layer of a silicon-on-insulator (SOI) wafer with $500\text{-}\mu\text{m}$ handle wafer thickness. Low-pressure chemical vapor deposition (LPCVD) deposited Si_3N_4 and polysilicon (poly-Si) are used to form a $2\text{-}\mu\text{m}$ -thick spacer around the perimeter of the backplate, and this spacer defines the nominal stand-off distance between the diaphragm and backplate in a subsequent two-chip assembly process. A much thinner layer of PECVD silicon nitride (SiN) was deposited on both front and back sides of the backplate in an effort to increase its stiffness, owing to residual tensile stress in the film. The layered cross-section is depicted schematically in Fig. 5, and micrographs and scanning-electron-microscope (SEM) images of the actual device are presented at various zoom levels in Fig. 6.

As best observed in Figs. 6(a) and 6(b) which show the topside of the grating die, the backplate is comprised of highly perforated honeycomb-shaped cells, with an optical-diffraction grating fabricated at the center. The grating has $4\text{-}\mu\text{m}$ pitch and approximately 50% fill factor. The diaphragm is fabricated on a separate SOI wafer, also with a $2\text{-}\mu\text{m}$ Si device layer thickness. The diaphragm layer was subsequently reduced using a timed reactive-ion etching (RIE) process to yield $1.5\text{-}\mu\text{m}$ -thick diaphragms, which was

done to increase diaphragm compliance. An alternative to this final diaphragm-thinning step would be to begin the diaphragm fabrication process with the desired diaphragm thickness of $1.5\text{ }\mu\text{m}$. In this research, use of a $2\text{-}\mu\text{m}$ -thick diaphragm assisted in increasing device yield through the KOH-etching process as well as the subsequent wafer-dicing operation. Less than $2\text{-}\mu\text{m}$ -thick device layer becomes problematic when the KOH etching is completed since the excessive residual stress between the device layer and buried- SiO_2 layer damages diaphragms. For the same reason, the KOH-etched wafer was submerged to buffered-oxide etch (BOE) solution to remove the exposed buried- SiO_2 layer. After the SiO_2 removal, the wafer was diced to individual dies. Both backplate and diaphragm dies were coated with a 100-nm -thick Au layer, as illustrated in Fig. 5, to increase optical reflectivity. In a final sealing step, both dies were bonded against each other to form the completed assembly. SEM images of the completed assembly are presented from the backside in Figs. 6(c) and 6(d).

IV. SYSTEM IDENTIFICATION VIA ELECTROSTATIC RESPONSE MEASUREMENTS

The ultimate goal in experimentally evaluating the prototype is to understand the dynamics of the microphone structure. Specifically, the aim is to experimentally extract

FIG. 6. (Color online) Micrographs of (a) the topside and (b) grating region of the backplate die, and SEM images of (c) the prototype from the backside and (d) the magnified view of the grating region.

the parameters in a lumped-network model that can accurately predict (1) the frequency response of the diaphragm in response to uniform sound pressure and (2) the backplate-induced thermal-mechanical displacement noise and its input-pressure-referred noise spectrum. Extracting this information from the isolated diaphragm-backplate subsystem will assist in modeling and understanding observations from future full system-integrated prototypes. The ability to electrostatically actuate the diaphragm provides two useful features: (1) DC forces can be used to control the position of the microphone diaphragm to control sensitivity as demonstrated below, and (2) broadband AC forces can be used to characterize the dynamic frequency response of the diaphragm/backplate system over a broad frequency range, and this response can, in-turn, be used to extract important system parameters.

The system as depicted schematically in the Fig. 5 shows separate wire-bonds attached to both handle wafers. These connections enable electrostatic (ES) forces to be applied between the grating and the diaphragm via capacitive coupling through the buried SiO₂ layers. The capacitances formed by the pair of buried SiO₂ layers are large in comparison to the device capacitance so a large majority of the applied ES voltage appears across the device capacitance as desired. While actuating through the buried SiO₂ layers of the SOI wafers works well for prototype testing, potential charge trapping and dielectric leakage in the buried SiO₂ layers can cause fluctuations in the DC charge held across the diaphragm and backplate, which can affect the long-term stability of the device by causing a drift in the DC electrostatic actuation force.⁴⁶ A structure in which electrical contacts are made directly to the diaphragm and backplate is recommended in future embodiments. No such leakage problems were observed in prototype testing. The device-under-test (DUT) is configured for optical readout as presented in Fig. 7(a). An 850-nm-wavelength VCSEL with an integrated lens illuminates the diaphragm/grating system, and a three-element PD array with 3.25-mm pitch captures the zeroth-order beam-intensity with the center PD, and the sum of the $\pm$ first-order beam-intensities with the two exterior PDs. Photocurrents are amplified and buffered with the transimpedance amplifier (TIA) configuration presented in Fig. 7(b). In a first experiment, the optical-readout and ES-actuation capabilities are tested by applying a slow-varying (100-Hz) triangle-wave actuation waveform (0-V to 25-V amplitude) as captured and presented in Fig. 8. Units for the actuation signal are shown on the right y-axis. The resulting center-and-outer beam signals are simultaneously recorded and plotted on the same trace, with units on the left y-axis. The beam intensities are sinusoidal and complimentary as expected. The peak in modulation efficiency in the actuation voltage range between 10 and 15 V is likely due to favorable curvature of the diaphragm/grating system in this bias range. In this region, modulation efficiency, computed as (peak-min)/peak, is equal to 72%.

The frequency response of the microphone structure to ES input is obtained by first biasing the diaphragm-grating gap height to the gap corresponding to point *B* on the zeroth-order PD interference curve, which is a point of maximum

(a)

(b)

FIG. 7. (Color online) (a) Prototype configured for measurements in anechoic chamber and (b) TIA circuit diagrams for the zeroth-order and $\pm$ first-order photodiodes.

slope and linearity for reading out small vibrations of the diaphragm. The corresponding DC-bias voltage is approximately 12 V as can be discerned from Fig. 8. A small white-noise ES input voltage was also applied, and the amplitude of the zeroth-order-PD fast Fourier Transform (FFT) was captured as the ES frequency response. The measurement sequence was automated using the “bin-centers” frequency response measurement tool in a PrismSound dScope series III audio analyzer. The audio analyzer generates a white-noise like signal with the noise power in each frequency bin regulated precisely by the analyzer. This stimulus is applied to the DUT, and the resultant signal from the device is measured and synchronized to the device input signal. The synchronized input/output signal pair is used to establish the device transfer function by taking the FFT of the output and normalizing to the FFT of the input. For this particular measurement, a 2.93-Hz bin width was used and the measurement range was 2.93 Hz to 100 kHz. The measurement is repeated and averaged 50 times to produce the measurements presented in Fig. 9. The measured ES frequency response contains two prominent peaks at 16.3 kHz and

FIG. 8. (Color online) Optical readout with electrostatic actuation.

70 kHz, corresponding to the diaphragm and backplate fundamental resonances, respectively, as discussed further below.

The relevant network model for the microphone structure is presented in Fig. 10. Since the structure is not fully packaged, only a subset of the elements presented in Fig. 1 are relevant. Specifically, referring to Fig. 1, C_{bc} and C_{fc} are infinite and the port impedances are equal to zero, resulting in the model in Fig. 10. The model is presented using mechanical parameters with force and velocity the effort and flow variables, respectively. Both electrostatic and external force inputs are included. The external input, F_{acst} , represents a diaphragm force in response to uniform acoustic pressure. In a completed package, F_{acst} is the differential pressure between front and back cavities ($P_{fc} - P_{bc}$) scaled by the effective area of the diaphragm. F_{acst} is set equal to zero for ES response simulations. The effective mass (i.e., modal mass) and resonance frequencies of the diaphragm and backplate are modeled using a finite-element-analysis (FEA) software, ANSYS. Residual tensile stresses in the epitaxial Si

FIG. 9. (Color online) Measured and simulated frequency response with electrostatic actuation of the prototype.

FIG. 10. (Color online) Simplified equivalent circuit diagram of the prototype device.

layer of the diaphragm and in the SiN film on the backplate are included in the FEA model. The fundamental resonance frequency of the backplate in particular is strongly dependent on the residual stress in the SiN layer. With modal mass and resonance frequencies determined from ANSYS, the corresponding compliances are computed as $C_m = 1/m\omega_n^2$. Residual stress in the diaphragm and in the backplate SiN layer provide fitting parameters used for fitting modeled resonance frequencies to measured resonance frequencies as extracted from the ES frequency response data. A FEA model for R_m , the mechanical flow resistance of the backplate and grating, has also been developed and described in detail in prior work. The majority of the backplate damping is caused by flow through the grating, owing to the small 2- μ m-wide slots as compared to the large open-region of the honeycomb-shaped perforations.

Since the DUT is not fully packaged, acoustic radiation impedance on the diaphragm, R_d , is also included in the circuit model. The mechanical radiation impedance is modeled using a baffled-piston approximation for the diaphragm. To address the non-uniform deformation and velocity profile of the diaphragm, the radiation impedance is computed using the area-averaged diaphragm velocity rather than the peak velocity which occurs at the diaphragm center. For the diaphragm under study, $v_{avg} = 0.227v_{peak}$.

A mesh analysis is performed with the circuit model shown in Fig. 10, and the derived linear equations from the mesh analysis are solved for v_d and v_{bp} using MATLAB.

$$\begin{bmatrix} j\omega m_d + \frac{1}{j\omega C_d} + R_d + R_m & -R_m \\ -R_m & j\omega m_{bp} + \frac{1}{j\omega C_{bp}} + R_m \end{bmatrix} \times \begin{bmatrix} v_d \\ v_{bp} \end{bmatrix} = \begin{bmatrix} -F_{acst} + F_{es} \\ -F_{es} \end{bmatrix}. \quad (1)$$

The simulated ES response is presented in Fig. 9 along with measured data. As mentioned above, F_{acst} is set to zero for the ES response simulation. A residual SiN stress equal to 165 MPa results in an excellent match to the measured backplate resonance frequency of 70 kHz. No fitting is necessary

TABLE I. Measured parameters from the frequency response measurement with electrostatic actuation.

Variables	Descriptions	Values
f_d	Diaphragm resonance frequency	16.3 kHz
Q_d	Diaphragm resonance quality factor	17.9
f_{bp}	Backplate resonance frequency	70.0 kHz
Q_{bp}	Backplate resonance quality factor	10.72

for R_m , as the modeled value results in a nearly perfect Q prediction for both diaphragm and backplate resonances. Table I summarizes the measured response parameters, and Table II summarizes the extracted network parameters. The radiation impedance resistance, R_d , is $18\times$ smaller than R_m at the diaphragm resonance frequency of 16.3 kHz and does therefore not significantly affect the resonance Q and/or thermal-mechanical noise spectra over the frequency range studied.

A backplate resonance frequency between 70 and 80 kHz is consistent with results obtained in a recent study, in which a similar experimental setup was used to isolate the backplate dynamics. Specifically, the compliant diaphragm in Fig. 5 was replaced by a rigid bulk-silicon reflector with a polished surface, and ES actuation was used to measure the backplate resonance frequency and Q . Referring to the network model in Fig. 10, this corresponds to “open-circuiting” the diaphragm branch, and a simple RLC -circuit remains for the backplate. The study enabled a direct measurement of backplate resonance and Q , and in-turn a system parameter extraction of R_m , C_{bp} , and m_{bp} . The measured backplate resonance was equal to 72.4 kHz, which is close to the backplate used in this work.

V. NOISE SIMULATIONS: PRELIMINARY REMARKS REGARDING BACKPLATE COMPLIANCE

With the network parameters experimentally extracted through ES response data, the suitability of the structure in Fig. 6 for a low-noise microphone can be assessed after the noise model is established. The thermal-mechanical forces induced by R_m and R_d in units of $N/\sqrt{\text{Hz}}$ are computed by (2) and (3), respectively.

$$F_{tm,R_m} = \sqrt{4k_b T R_m}, \quad (2)$$

$$F_{tm,R_d} = \sqrt{4k_b T R_d}, \quad (3)$$

where k_b is the Boltzmann constant, and T is ambient temperature.¹⁹ These noise generators are included in the

TABLE II. Extracted model parameters.

Variables	Descriptions	Values
m_d	Diaphragm effective mass	0.81×10^{-9} kg
C_d	Diaphragm compliance	0.12 m/N
m_{bp}	Backplate effective mass	0.14×10^{-9} kg
C_{bp}	Backplate compliance	0.037 m/N
R_m	Backplate flow resistance	7.3×10^{-6} N·s/m

network model of Fig. 10. The network may be analyzed in the frequency domain to obtain the equations required to solve for the backplate and diaphragm velocities,

$$\begin{bmatrix} j\omega m_d + \frac{1}{j\omega C_d} + R_d + R_m & -R_m \\ -R_m & j\omega m_{bp} + \frac{1}{j\omega C_{bp}} + R_m \end{bmatrix} \times \begin{bmatrix} v_d \\ v_{bp} \end{bmatrix} = \begin{bmatrix} \sqrt{F_{tm,R_d}^2 + F_{tm,R_m}^2} \\ F_{tm,R_m} \end{bmatrix}. \quad (4)$$

For the noise analysis, F_{acst} and F_{es} are set to 0. Equation (4) is readily solved by the linear solver provided in MATLAB for v_d and v_{bp} . The relative displacement between the diaphragm and backplate, d_{tm} , is then computed by $(1/j\omega)(v_{rel})$, where $v_{rel} = v_d - v_{bp}$. The relative displacement of the diaphragm with respect to the grating determines the thermal-mechanical displacement of the device since the optical-readout system detects the relative motion. It is insightful to simulate d_{tm} due to each noise force individually, and then use incoherent summation to obtain the total thermal-mechanical noise, d_{tm} , as,

$$d_{tm} = \sqrt{d_{tm,R_m}^2 + d_{tm,R_d}^2}, \quad (5)$$

where d_{tm,R_m} and d_{tm,R_d} are the thermal-mechanical displacements due to F_{tm,R_m} and F_{tm,R_d} , respectively.

For a low-noise microphone, an ideal design would employ a rigid backplate. In this prototype, this ideal goal is not achieved. The ratio of C_{bp} to C_d is 0.32, following the extracted parameters in Table II, i.e., the backplate is only $3.15\times$ stiffer than the diaphragm. Generally speaking, the movable backplate has two potential adverse effects: (1) As presented in Fig. 10, the movable backplate has the potential to reduce the relative motion between the diaphragm and backplate in the acoustically driven response (i.e., in the response to F_{acst}), and (2) the movable backplate contributes a displacement noise in response to thermal-mechanical noise induced by R_m .

With respect to the former concern (1), the movable backplate has no effect at DC. Referring to the model in Fig. 10, the infinite impedance of C_{bp} at low frequencies forces flow through R_m as desired. A “low frequency” analysis of the network in Fig. 10 reveals that significant motion through C_{bp} occurs only above a cut-off frequency defined by C_{bp} and R_m , as $f_{c,bp} = 1/(2\pi R_m C_{bp})$, which for this prototype is 583 kHz, well above the frequency range of interest. With respect to (2), the input-pressure-referred thermal-mechanical noise level is 32% higher (or 2.4 dB) than would be the case for a rigid backplate. The simplified network in Fig. 10 illustrates why this is the case. Referring to the network, it can be shown that Eqs. (6)–(9) are valid in the flat-band of the microphone where compliance elements dominate, i.e., these equations are obtained by setting inductive elements in the network to zero, and solving the system for d_{tm} due to F_{tm,R_m} .

$$d_{tm} = F_{tm,R_m}(C_d + C_{bp}), \quad (6)$$

$$P_{tm} = d_{tm}/(C_d A_{eff}) = F_{tm,R_m}(C_d + C_{bp})/(C_d A_{eff}), \quad (7)$$

$$= \frac{F_{tm,R_m}}{A_{eff}} \left(1 + \frac{C_{bp}}{C_d}\right), \quad (8)$$

$$= 1.32 \frac{F_{tm,R_m}}{A_{eff}}, \quad (9)$$

where A_{eff} is the effective area of the diaphragm and equal to approximately $0.227A_{act}$ for a square diaphragm with clamped boundaries, where A_{act} is the actual area of the diaphragm. Equation (7) and subsequently (8) and (9) refer the thermal-mechanical displacement noise d_{tm} to an equivalent uniform pressure acting on the diaphragm via dividing d_{tm} by the mechanical sensitivity of the diaphragm. For the presented prototype, and based on the extracted network parameters summarized in Table II, the predicted thermal-mechanical displacement noise is $54 \text{ fm}/\sqrt{\text{Hz}}$ following (6), and the predicted pressure noise in the flat band is $1.75 \text{ } \mu\text{Pa}/\sqrt{\text{Hz}}$ following (7), (8), or (9).

VI. NOISE MEASUREMENTS

Noise measurements are performed by taking the setup pictured in Fig. 7(a) into a $10 \text{ ft} \times 10 \text{ ft} \times 10 \text{ ft}$ walk-in acoustically anechoic test chamber and observing the zeroth-order-beam TIA output ($R_f = 50 \text{ k}\Omega$) spectrum using a PrismSound dScope Series III FFT audio analyzer. All noise measurements were performed using a Hamming window and a frequency bin width of 2.93 Hz , although it was verified that selection of a window setting, including a flat window, only affected the values of the low frequency FFT coefficients (below 20 Hz). To establish a baseline noise floor for the electronics, the TIA output from the zeroth-order PD was first recorded with the VCSEL turned off. The measured amplifier noise is presented in Fig. 11 along with the simulated amplifier noise spectrum which is in close agreement. The amplifier output noise is due to the combination of Johnson-Nyquist noise of the feedback resistor,

computed as $V_{n,R_f} = \sqrt{4k_b T R_f} = 28.7 \text{ nV}/\sqrt{\text{Hz}}$, and internal current noise of the op amp, computed as $V_{n,i_{n-}} = i_{n-} \times R_f = 35 \text{ nV}/\sqrt{\text{Hz}}$ for the Analog Devices, Inc. OP470GPZ op amp used with spec'd current noise of $i_{n-} = 0.7 \text{ pA}/\sqrt{\text{Hz}}$. The incoherent summation of these two voltage-noise spectral densities produces the simulated trace shown in Fig. 11. Next, the VCSEL power is turned on, and the output noise is measured at three different points along the interference curve presented in Fig. 8.

At point A, the displacement sensitivity of the measurement system is zero since the slope of the interference curve is zero at this bias point. The measured noise spectrum at this bias point is due to the combination of quantum shot-noise at the photodetectors and random intensity fluctuations in the laser light known as laser relative intensity noise (RIN). The measured DC output voltage of the TIA and the known feedback resistor ($R_f = 50 \text{ k}\Omega$) enables computation of the DC photocurrent at the PDs, which for the zeroth-order beam is $21.4 \text{ } \mu\text{A}$. The computed shot noise following $V_{shot} = R_f \sqrt{2qI_{DC}}$ is $131 \text{ nV}/\sqrt{\text{Hz}}$, where q is the elementary charge, and is also presented in Fig. 11. The measured noise spectrum only approaches shot noise at high frequencies (above 10 kHz), and is dominated by RIN elsewhere. The measured spectrum at point C, also a "dead" point with zero sensitivity, is presented in Fig. 11 as well. The RIN spectrum at point C is higher than the RIN spectrum at point A, as expected since RIN is linearly proportional to the amount of DC light captured at the PD. The measured spectrum at point B, a point of maximum displacement sensitivity, is markedly different. This spectrum is dominated by backplate-resistance induced thermal-mechanical noise at frequencies above 1 kHz , and by laser RIN at frequencies less than 1 kHz . The thermal-mechanical noise spectrum resembles the measured ES response in Fig. 9 as expected, showing both diaphragm and backplate resonance peaks. For clarity, the noise at bias point B is the only relevant spectrum during operation since that is the operating point of the microphone.

The measured output spectrum in $\text{V}/\sqrt{\text{Hz}}$ is more useful when referred to displacement, accomplished by dividing the TIA output spectrum by the measured displacement sensitivity. The measured displacement sensitivity with units of V/m is obtained as,

$$S_d = \frac{2\pi}{\lambda} V_{pp}, \quad (10)$$

where V_{pp} is the peak-to-peak swing in modulation and λ is the wavelength of incident laser light, or 850 nm in this case. Referring to the optical interference curve, the peak-to-peak swing is equal to the TIA output at point C minus the output at point A, or $3.82 - 1.07 = 2.75 \text{ V}$. For the device under study, the displacement sensitivity is therefore $20.3 \text{ mV}/\text{nm}$. Equation (6) is applicable to any Michelson-type interferometer, with additional details found in Refs. 37–39. The displacement noise spectrum is presented in Fig. 12. Also shown is the simulated displacement noise spectrum following the simple network model in Fig. 10 and using the parameters identified in Table II. The predicted relative

FIG. 11. (Color online) Measured and simulated noise spectral densities.

FIG. 12. (Color online) Measured and simulated thermal-mechanical noise spectral densities of the prototype.

diaphragm-grating displacement noise of $54 \text{ fm}/\sqrt{\text{Hz}}$ closely matches the measured spectra at frequencies above 1 kHz. The close agreement in measured noise amplitude provides independent verification of the system identification (ID) procedure described above based on the ES response measurements. While the system ID procedure based on the ES measurements relies on the known mass of the diaphragm and backplate, the absolute noise amplitude measured in Fig. 12 depends only on R_m , C_{bp} , and C_d , and it is independent of the diaphragm and backplate mass.

Finally, both the measured and simulated displacement noise can be referred to input sound pressure using the known diaphragm compliance and area, i.e., using $P_{tm} = d_{tm}/(C_d A_{eff})$. The pressured-referred noise units are displayed along the right y-axis in Fig. 12, yielding a measured noise equal to approximately $2 \mu\text{Pa}/\sqrt{\text{Hz}}$ at 1 kHz and above. The computed A-weighted pressure noise using the measured spectrum is 24.0 dBA. The RIN-dominated portion of the spectrum below 1 kHz is responsible for 1.4 dB, and the thermal-mechanical noise level for the assembled prototype is 22.6 dBA. It is anticipated that RIN can be

FIG. 13. (Color online) Measured thermal-mechanical noise with/without applying the A-weighting filter.

successfully cancelled using a differential-readout approach as previously demonstrated in Refs. 38 and 47. As shown in Fig. 13, the dominant noise effect of the RIN below 1 kHz becomes less significant once the A-weighting filter is applied to the noise spectrum. The importance of minimizing the thermal-mechanical noise from the device is clearly observable in the same figure which shows that the noise spectrum from 1 to 10 kHz is the major contribution to the overall noise level. As a final note, an anomaly in the measured data is the small narrowband noise peak at 90 Hz. It is speculated that this is due to ambient noise outside of the chamber from nearby construction, but this remains the subject of future measurements.

VII. DISCUSSION AND CONCLUSION

Referencing (9) and noting that $C_{bp} = 0.32C_d$, one observes that C_{bp} is responsible for adding 2.4 dB of thermal-mechanical noise. If the backplate can be made rigid relative to the diaphragm (e.g., through stiffening of the backplate with additional PECVD SiN layer), the resulting thermal-mechanical noise is expected to be 20.2 dBA. While such a design would already be a significant improvement compared to commercial condenser MEMS microphones, there exists a design path to yield additional improvements. The grating is responsible for the majority of damping, and the diameter of the grating in the presented design is $128 \mu\text{m}$. F_{tm} scales with grating diameter, so reducing the grating diameter from 128 to $64 \mu\text{m}$ is anticipated to yield 6-dB lower F_{tm} and a thermal-mechanical noise floor equal to 14.2 dBA, and therefore a microphone with approximately 80-dB SNR.

In the presented work, a path towards 80-dB SNR MEMS microphones has been identified. Only a subsystem of the total microphone system has been assembled and studied in this work. While the complete surface-mount style packaging of optical microphones has been demonstrated in previous work,²³ this particular study only included the microphone diaphragm and backplate. It has been argued through simulation that noise introduced by vents and inlet ports in fully packaged microphones do not preclude the realization of ultra-low noise MEMS microphones, and that the diaphragm-backplate subsystem studied in this work has an advantage over capacitive microphones with respect to achieving sub 20-dBA miniature microphones.

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¹G. M. Sessler, "Acoustic sensors," *Sensors Actuators A* **26**, 323–330 (1991).

²P. R. Scheeper, A. G. H. van der Donk, W. Olthuis, and P. Bergveld, "A review of silicon microphones," *Sensors Actuators A* **44**, 1–11 (1994).

- ³M. D. Williams, B. A. Griffin, T. N. Reagan, J. R. Underbrink, and M. Sheplak, "An AIN MEMS piezoelectric microphone for aeroacoustic applications," *J. Microelectromech. Syst.* **21**, 270–283 (2012).
- ⁴L. Baumgartel, A. Vafanejad, C. Shih-Jui, and K. Eun Sok, "Resonance-enhanced piezoelectric microphone array for broadband or prefiltered acoustic sensing," *J. Microelectromech. Syst.* **22**, 107–114 (2013).
- ⁵P. R. Scheeper, B. Nordstrand, J. O. Gullv, L. Bin, T. Clausen, L. Midjord, and T. Storgaard-Larsen "A new measurement microphone based on MEMS technology," *J. Microelectromech. Syst.* **12**, 880–891 (2003).
- ⁶P. V. Loeppert and S. B. Lee, "SiSonic™—The first commercialized MEMS microphone," in *Proceedings of the Solid-State Sensors, Actuators, and Microsystems Workshop* (Hilton Head Island, SC, 2006), pp. 27–30.
- ⁷J. W. Weigold, T. J. Brosnihan, J. Bergeron, and X. Zhang, "A MEMS condenser microphone for consumer applications," in *19th IEEE International Conference on Micro Electro Mechanical Systems* (Istanbul, Turkey, 2006), pp. 86–89.
- ⁸M. W. Alfons Dehé, M. Földner, and U. Krumbein, "The infineon silicon MEMS microphone," in *AMA Conferences 2013* (AMA Service GmbH, Nuremberg, Germany, 2013), pp. 95–99.
- ⁹J. Liu, D. T. Martin, K. Kadirvel, T. Nishida, L. Cattafesta, M. Sheplak, and B. P. Mann, "Nonlinear model and system identification of a capacitive dual-backplate MEMS microphone," *J. Sound Vib.* **309**, 276–292 (2008).
- ¹⁰J. Bay, O. Hansen, and S. Bouwstra, "Design of a silicon microphone with differential read-out of a sealed double parallel-plate capacitor," *Sensors Actuators A* **53**, 232–236 (1996).
- ¹¹A. Torkkeli, O. Rusanen, J. Saarilahti, H. Seppä, H. Sipola, and J. Hietanen, "Capacitive microphone with low-stress polysilicon membrane and high-stress polysilicon backplate," *Sensors Actuators A* **85**, 116–123 (2000).
- ¹²M. Pederson, W. Olthuis, and P. Bergveld, "High-performance condenser microphone with fully integrated CMOS amplifier and DC-DC voltage converter," *IEEE J. Microelectromech. Syst.* **7**, 387–394 (1998).
- ¹³P. R. Scheeper, W. Olthuis, and P. Bergveld, "A silicon condenser microphone with a silicon nitride diaphragm and backplate," *J. Micromech. Microeng.* **2**, 187 (1992).
- ¹⁴M. Földner and A. Dehé, "Challenges of high SNR (signal-to-noise) silicon micro machined microphones," in *Proceedings of 19th International Congress on Acoustics*, Madrid, Spain (2007), pp. 1–6.
- ¹⁵S. C. Thompson, J. L. LoPresti, E. M. Ring, H. G. Nepomuceno, J. J. Beard, W. J. Ballard, and E. V. Carlson, "Noise in miniature microphones," *J. Acoust. Soc. Am.* **111**, 861–866 (2002).
- ¹⁶A. Dehé, "Silicon microphone development and application," *Sensors Actuators A* **133**, 283–287 (2007).
- ¹⁷H. Nyquist, "Thermal agitation of electric charge in conductors," *Phys. Rev.* **32**, 110–113 (1928).
- ¹⁸H. B. Callen and T. A. Welton, "Irreversibility and generalized noise," *Phys. Rev.* **83**, 34–40 (1951).
- ¹⁹T. B. Gabrielson, "Mechanical-thermal noise in micromachined acoustic and vibration sensors," *IEEE Trans. Electron Dev.* **40**, 903–909 (1993).
- ²⁰Analog Devices, Inc. ADMP504, available at http://www.knowles.com/search/prods_pdf/SPV0840LR5H-B.pdf (Last viewed September 17, 2013).
- ²¹Knowles Electronics, LLC. Zero-Height SiSonic™ Microphone (SPV0840LR5H-B), available at http://www.knowles.com/search/prods_pdf/SPV0840LR5H-B.pdf (Last viewed September 17, 2013).
- ²²ANSI S1.42-2001: *American National Standard Design Response of Weighting Networks for Acoustical Measurements* (Acoustical Society of America, New York, 2001).
- ²³M. L. Kuntzman, C. T. Garcia, A. G. Onaran, B. Avenson, K. D. Kirk, and N. A. Hall, "Performance and modeling of a fully packaged micromachined optical microphone," *J. Microelectromech. Syst.* **20**, 828–833 (2011).
- ²⁴L. L. Beranek, *Acoustics* (American Institute of Physics, Woodbury, New York, 1996).
- ²⁵D. Homentcovschi and R. N. Miles, "Modeling of viscous damping of perforated planar microstructures. Applications in acoustics," *J. Acoust. Soc. Am.* **116**, 2939–2947 (2004).
- ²⁶D. Homentcovschi and R. N. Miles, "Viscous damping of perforated planar micromechanical structures," *Sensors Actuators A* **119**, 544–552 (2005).
- ²⁷M. Bao and H. Yang, "Squeeze film air damping in MEMS," *Sensors Actuators A* **136**, 3–27 (2007).
- ²⁸J. Bergqvist, "Finite-element modeling and characterization of a silicon condenser microphone with a highly perforated backplate," *Sensors Actuators A* **39**, 191–200 (1993).
- ²⁹S. P. Thompson, "The photophone," *Nature* **22**, 481 (1880).
- ³⁰N. Bilaniuk, "Optical microphone transduction techniques," *Appl. Acoust.* **50**, 35–63 (1997).
- ³¹J. A. Bucaro, N. Lagakos, B. H. Houston, J. Jarzynski, and M. Zalalutdinov, "Miniature, high performance, low-cost fiber optic microphone," *J. Acoust. Soc. Am.* **118**, 1406–1413 (2005).
- ³²A. Hu, F. W. Cuomo, and A. J. Zuckewar, "Theoretical and experimental study of a fiber optic microphone," *J. Acoust. Soc. Am.* **91**, 3049–3056 (1992).
- ³³E. G. Bakhoun and M. H. M. Cheng, "Advanced optical microphone," *IEEE Sensors J.* **14**, 7–14 (2014).
- ³⁴B. A. Griffin, D. A. Mills, T. Schmitz, and M. Sheplak, "Fabrication and characterization of a sapphire based fiber optic microphone for harsh environments," *J. Acoust. Soc. Am.* **128**, 2444 (2010).
- ³⁵M. L. Kuntzman, K. D. Kirk, C. T. Garcia, G. A. Onaran, and N. A. Hall, "Commercial packaging of an optical microelectromechanical systems microphone," *J. Acoust. Soc. Am.* **128**, 2444 (2010).
- ³⁶N. A. Hall, M. Okandan, R. Littrell, B. Bicen, and F. L. Degertekin, "Micromachined optical microphone structures with low thermal-mechanical noise levels," *J. Acoust. Soc. Am.* **122**, 2031–2037 (2007).
- ³⁷N. A. Hall, B. Bicen, M. K. Jeelani, W. Lee, S. Qureshi, F. L. Degertekin, and M. Okandan, "Micromachined microphones with diffraction-based optical displacement detection," *J. Acoust. Soc. Am.* **118**, 3000–3009 (2005).
- ³⁸W. Lee, N. A. Hall, Z. Zhiping, and F. L. Degertekin, "Fabrication and characterization of a micromachined acoustic sensor with integrated optical readout," *IEEE J. Selected Topics Quant. Electron.* **10**, 643–651 (2004).
- ³⁹N. A. Hall and F. L. Degertekin, "Integrated optical interferometric detection method for micromachined capacitive acoustic transducers," *Appl. Phys. Lett.* **80**, 3859–3861 (2002).
- ⁴⁰C. Weili, B. Bicen, N. Hall, S. A. Jones, F. L. Degertekin, and R. N. Miles, "Optical Sensing In a directional Memsmicrophone Inspired by the Ears of the Parasitoid Fly, *Ormia ochracea*," in *2006 IEEE 19th International Conference on Micro Electro Mechanical Systems (MEMS)* (IEEE, Istanbul, Turkey, 2006), pp. 614–617.
- ⁴¹B. Bicen, S. Jolly, K. Jeelani, C. T. Garcia, N. A. Hall, F. L. Degertekin, S. Quang, C. Weili, and R. N. Miles, "Integrated optical displacement detection and electrostatic actuation for directional optical microphones with micromachined biomimetic diaphragms," *IEEE Sens. J.* **9**, 1933–1941 (2009).
- ⁴²Z. Parlak and F. L. Degertekin, "Combined quantitative ultrasonic and time-resolved interaction force AFM imaging," *Rev. Sci. Instr.* **82**, 013703 (2011).
- ⁴³N. A. Hall, W. Lee, and F. L. Degertekin, "Capacitive micromachined ultrasonic transducers with diffraction-based integrated optical displacement detection," *IEEE Trans. Ultrason. Ferroelectr. Frequency Cont.* **50**, 1570–1580 (2003).
- ⁴⁴C. Garcia, G. Onaran, B. Avenson, M. Christensen, A. Liu, N. Hewa-Kasakarage, and N. Hall, "Micro-Seismometers via advanced meso-scale fabrication," in *Monitoring Research Review* (Tucson, AZ, 2011), pp. 274–282.
- ⁴⁵N. A. Hall, M. Okandan, R. Littrell, D. K. Serkland, G. A. Keeler, K. Peterson, B. Bicen, C. T. Garcia, and F. L. Degertekin, "Micromachined accelerometers with optical interferometric read-out and integrated electrostatic actuation," *J. Microelectromech. Syst.* **17**, 37–44 (2008).
- ⁴⁶W. Merlijn van Spengen, "MEMS reliability from a failure mechanisms perspective," *Microelectron. Reliab.* **43**, 1049–1060 (2003).
- ⁴⁷R. Littrell, N. A. Hall, M. Okandan, R. Olsson, and D. Serkland, "Impact of relative intensity noise of vertical-cavity surface-emitting lasers on optics-based micromachined audio and seismic sensors," *Appl. Opt.* **46**, 6907–6911 (2007).