Studies on a Piezoelectric Cylindrical Transducer for Borehole Dipole Acoustic Measurements

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Abstract: In this article, a novel design of a piezoelectric dipole transducer is proposed for formation acoustic velocity measurement in the vicinity of a borehole with a frequency range of 0.4–6 kHz. The transducer which actuates a cylindrical shell to generate a pure dipole mode wave by using multiple piezoelectric bender bars is analyzed theoretically and simulated numerically by using the finite element method (FEM). Moreover, the transducer is fabricated and tested to compare with the numerical simulation results, which shows that the test and simulation results are in good agreement. Finally, compared with numerical simulation results of the traditional dipole transducer, it is shown that the proposed dipole transducer has higher transmitting sensitivities than commonly used ones, especially in low frequency responses. This work lays a foundation for the new development of the transducer in borehole dipole acoustic shear wave measurements. Especially, in a slow formation where the shear wave velocity is lower than that of compressional wave in the borehole fluid, the transducer could be used for highly efficient shear wave velocity measurements.

Keywords: piezoelectric bender bar; borehole acoustic measurements; cylindrical shell; dipole transducer

1. Introduction

Borehole acoustic wave logging or acoustic wave measurement is used to predict formation lithology, evaluate the formation porosity, and indicate the pore fluid properties in an open hole by measuring the compressional and shear velocities of the formation in the vicinity of a borehole [1]. In addition, these measurements could be used to characterize the geo-stress field in horizontal directions [2–4].

In a slow formation in which the shear wave velocity is smaller than that of borehole fluid, borehole dipole acoustic measurement has been used for generating flexural mode waves propagating along the borehole, and these mode waves, during propagation, carry the shear wave events of the formation outside the borehole, and can be used for picking up the shear velocity of the formation [5]. In order to procure the accurate shear velocity and obtain the dispersion of flexural mode waves effectively, dipole transducers are required with a wide frequency bandwidth and a high radiation power. Moreover, the transducer should be able to work against high temperature and pressure in a borehole, with temperatures of 175 °C and pressures of 140 MPa.

Since 1968, various dipole acoustic transducers have been proposed and applied. One of the most conventional, piezoelectric bender-type transducers [6–9], are extensively used in the borehole dipole acoustic measurement tools [10]. However, they have the disadvantage of limited bandwidth in low frequency [10,11]. Moreover, the bender-type transducers easily produce hexapole aliasing because of the limited azimuthal angle of the opening window of the tool body in high frequency [10]. Although there are some other reports on borehole dipole acoustic transducers, which are not widely used in practice, a
dipole transducer that has a wide bandwidth of operation and a high modal purity is still in urgent need [12].

Based on the piezoelectric bender bars [8], a new dipole transducer for borehole acoustic measurement, effective to produce dipole signals in the frequency range of 0.4–6 kHz, is proposed and studied. In our article, four sections are included: principles and numerical analyses of the actuator and the whole transducer; comparison between the test and simulation results of the new transducer; comparison between the simulation results of traditional and new transducers; and conclusions.

2. Principles and Numerical Analyses

2.1. BENDER Bar and Its Vibration Principles

In this section, we first introduce the transducer vibration principles, and also conducted numerical analyses of the actuator and the whole transducer.

A piezoelectric bender bar is shown in Figure 1. A metal layer and two piezoelectric ceramic layers are bonded at the two interfaces. The x-axis is parallel to both the thickness direction of the bender bar and the polarization direction of both piezoelectric ceramic layers. The y-axis is parallel to the length direction of the bender bar and the bar is symmetric about the x-y-plane. The middle plane of the bender bar bisects its thickness. Its vibration principle is that when a voltage is applied to both surfaces of each piezoelectric element, one extends and the other one contracts, which produces the bending motion of the whole bender bar [13,14].

![Figure 1. Geometry of a trilaminar bender bar.](image)

In our previous work [15], we considered the theory of the small deflection of a thin plate. Because the surfaces of the bender bar are traction-free, it is assumed that the stresses in the volume vanish, except for stress $T_{xx}$, $T_{xy}$ and $T_{yy}$, i.e., $T_{xx} = T_{yy} = T_{zz} = 0$. The displacement in the z-direction due to bending of the bar is denoted as $w$. We divided the bender bar into two parts; one is composite three-layer bender with two piezoelectric ceramic layers and one metal layer, and the other one is the isotropic metal layer part. In the middle composite layer part ($|x| < L_p / 2$), the piezoelectric equations are as follows:

$$
\begin{align*}
T_1 &= \frac{1}{s_{11}^{2D}} S_1 + \frac{\sigma_p}{s_{11}^{D}} S_2 - \frac{g_{31}^S}{s_{11}^{D}} D_3, \\
T_2 &= \frac{1}{s_{11}^{2D}} S_1 + \frac{1}{s_{11}^{D}} S_2 - \frac{g_{31}^S}{s_{11}^{D}} D_3, \\
T_6 &= \frac{1}{s_{11}^{2D}} S_6, \\
E_3 &= -\frac{g_{31}^S}{s_{11}^{D}} (S_1 + S_2) + \frac{1}{s_{11}^{D}} D_3,
\end{align*}
$$

(1)

where $T_1, T_2, T_6$ are the stress components; $S_1, S_2, S_6$ are the strain components; $\sigma_p = s_{12}^{D}/s_{11}^{D}$ is the Poisson’s ratio of piezoelectric material when electric displacement is constant; $s_{11}^{2D}, s_{11}^{D}$ is the compliance coefficient component; $g_{31}^S$ is the dielectric isolation rate component; $g_{31}^S$ is the piezoelectric constant component; and $E_3, D_3$ are the electric field intensity component and electric displacement component, respectively.
For the isotropic metal layer part \( (L_p/2 < |x| < L_m/2) \), the stress and strain equations are shown as follows [15]:

\[
\begin{align*}
T'_1 &= \frac{E_m}{1-v_m^2} (S_1 + \sigma_m S_2), \\
T'_2 &= \frac{E_m}{1-v_m^2} (\sigma_m S_1 + S_2), \\
T'_6 &= \frac{2(1+v_m)}{2(1-v_m)} S_6,
\end{align*}
\]

(2)

where \( E_m \) is the Young’s modulus of metal material, and \( \sigma_m \) is the Poisson’s ratio of metal material.

By deriving the formula in our previous work, we obtained the vibration equation of the composite layer:

\[ D \nabla^2 \nabla^2 w + \rho H \frac{\partial^2 w}{\partial t^2} = 0, \]

(3)

where \( \nabla^2 = \frac{\partial^2}{\partial x^2} + \frac{\partial^2}{\partial y^2} \), \( D \) is the stiffness coefficient of composite layer; \( H = 2H_p + H_m \), with \( H_p, H_m \) as the thickness of the piezoelectric material and the metal material, respectively; \( \rho = 2\rho_p \frac{H_p}{H} + \rho_m \frac{H_m}{H} \) is the average density of the composite layer, with \( \rho_p, \rho_m \) being the density of the piezoelectric material and the metal material, respectively.

For the metal layer at both sides \( (L_p/2 < |x| < L_m/2) \), the vibration equation is:

\[ D_m \nabla^2 \nabla^2 w + \rho_m H_m \frac{\partial^2 w}{\partial t^2} = 0, \]

(4)

where \( D_m = \frac{E_m H_m^2}{12(1-v_m^2)} \) is the stiffness coefficient of the metal layer.

Suppose that the usual excitation is symmetric, the symmetrical vibration modes of bender bar are excited. For Equation (3) of the composite layer, the displacement is denoted as:

\[ w_c(x) = A_1 \cosh \frac{\omega}{v_1} x + C_1 \cos \frac{\omega}{v_1} x, \]

(5)

where \( v_1 = \sqrt{\frac{\omega D}{\rho_p H}} \), \( A_1, C_1 \) are constants to be determined.

For Equation (4) of the isotropic metal part, the displacement is denoted as:

\[ w_m(x) = A_2 \cosh \frac{\omega}{v_2} x + B_2 \sinh \frac{\omega}{v_2} x + C_2 \cos \frac{\omega}{v_2} x + D_2 \sin \frac{\omega}{v_2} x, \]

(6)

where \( v_2 = \sqrt{\frac{\omega D_m}{\rho_m H_m}} \), \( A_2, B_2, C_2, D_2 \) are constants to be determined.

Considering the continuity boundary condition at the junction between the composite layer and the metal layer, the physical function such as the transverse displacement, the shear force, the rotation angle, and the bending moment are continuous [15]. Moreover, the fixed boundary conditions need to be met at both of the ends of the bender bar. After substituting transverse displacement expressions in Equations (5) and (6) into continuity and fixed boundary conditions, we obtained the following coefficient matrix equations:

\[ a_{ij} A_i = c_i, i, j = 1, 2, \ldots 6. \]

(7)

According to the coefficient determinant value \( |a_{ij}| = 0 \), we calculated the resonant frequencies and corresponding vibration modes.

For the long and short benders that we used to actuate our cylindrical oscillating transducer, the resonant frequencies were 402 Hz, 3692 Hz, 767 Hz and 4705 Hz, calculated by our theory. The first two frequencies corresponded to the first and third orders of the bending modes of the long bender, and the latter two frequencies corresponded to those of the short bender. We used the finite element method to simulate vibration modes of the bender bar, as shown in Figure 2.
where the shear force, the rotation angle, and the bending moment are continuous [15]. Moreover, the fixed boundary conditions need to be met at both of the ends of the bender bar.

The dipole transducer consists of an internal piezoelectric actuator and external radiation structures. The internal actuator consists of three parallel piezoelectric benders, one of which is longer in the center, and the other two are shorter and of a same size. Each bender consists of two layers of piezoelectric elements and one layer of metal base elements. Between the site layer and the metal layer, the physical function such as the transverse displacement, the frequency range, and to extend the bandwidth of the dipole transducer, we proposed a new transducer design that we have patented in ref [16], which is shown in Figure 3.

For the traditional piezoelectric dipole source in borehole acoustic measurement, due to the limited strain of the piezoelectric ceramic material itself, it is not easy to generate large displacement at low frequencies [10,11]. Therefore, it is hard to enhance the transmitting voltage response at low frequencies, especially at frequencies around less than 1 kHz.

In addition, the frequency band of traditional source is not wide enough that it can measure shear wave velocity in soft formations.

Therefore, in order to improve its radiation performance, especially in the low frequency range, and to extend the bandwidth of the dipole transducer, we proposed a new transducer design that we have patented in ref [16], which is shown in Figure 3.

The schematic diagram and the sectional view along the vertical axis of the piezoelectric cylindrical transducer with its frame is shown in Figure 3. The rectangular coordinate system is set up in the acoustic center of the transducer. The $y$-axis and $z$-axis are shown in Figure 3, and the $x$-axis is in the inward direction perpendicular to the paper.
The dipole transducer consists of an internal piezoelectric actuator and external radiation structures. The internal actuator consists of three parallel piezoelectric benders, one of which is longer in the center, and the other two are shorter and of a same size. Each bender consists of two layers of piezoelectric elements and one layer of metal base element, which is commonly used in practice [8]. As shown in Figure 3, three layers are bonded at the two interfaces. The two ends of three parallel positioned benders are fixed to the endcaps bonded with the cylindrical shell. The external radiation structures consist of a cylindrical shell, two endcaps, and two U-shaped springs. The chamber, formed by the cylindrical shell and two endcaps, is filled with air. The cylindrical shell is supported by two U-shaped springs with the end faces fixed to the outside frame of a logging tool. The entire system is immersed in the fluid of the borehole. For numerical modeling, the whole structure is set to be immersed in air or water. During the operation, when a voltage is applied to the electrodes of the two piezoelectric elements, one piezoelectric element elongates and the other one contracts, causing the piezoelectric bender to bend. This makes the cylindrical shell oscillate out of the phase with the inner piezoelectric actuator.

The cylindrical shell, acting as the radiation surface of the transducer, is driven by the piezoelectric actuator to produce a pure dipole signal. Because the vibration of the piezoelectric actuator is confined in the space of cylindrical housing, it will not be harmful to the force imparted to the borehole wall [17]. Suppose that a force $F$ is applied to a borehole wall through borehole fluid by a force $F_1$ from the inner piezoelectric actuator to the cylindrical radiation surface. According to the Newton’s third law, a force $F_2$, equivalent in magnitude but opposite in direction to the force $F_1$ is applied to the piezoelectric actuator, and an anti-reaction force harmful to the force $F$ would not be imparted to the side wall of the borehole because the inner actuator is fixed in the cylindrical chamber and separated from the borehole fluid. Thus, any waves due to the reaction are not radiated toward the borehole wall [17]. On the other hand, for the conventional bender-type transducers, together with their frame, the opening angle of the sonic window is about 60 degrees due to the limited radiation surface. However, for the proposed transducer, with a full azimuthal angle of radiation surface, the opening angle of the sonic window can be as large as 120 degree or even more, which is efficient to inhibit the hexapole mode in a high frequency range [10]. Compared with the traditional bender transducer, the radiation structure of the cylindrical shell in the transducer greatly increases the radiation area, adapting to the size of the cylindrical borehole as much as possible, and thus effectively enhances the acoustic radiation resistance and transmitting voltage response.

The transducer vibration modes and responses are simulated by using the FEM. Firstly, the physical model of the transducer is set up; then, the simulation and analyses of electrical conductance, transmitting voltage response, and directivity patterns are conducted, respectively, to illustrate the effectiveness of the transducer for generating acoustic waves in the frequency range of 0.4–6 kHz. Finally, the optimization results are discussed. The finite element simulation model of the piezoelectric cylindrical transducer associated with its frame is shown in Figure 4.

2.3. Input Electrical Conductance in Air

The admittance can be calculated by using the following formula from ref [18]:

$$Y = \frac{dQ}{dt} = j\omega \frac{Q}{V} = j2\pi f \frac{Q}{V},$$

where $Q$ is the electric charge; $V$ is the excited voltage; $\omega$ is the angular frequency; and $f$ is the frequency. The conductance $G$ and susceptance $B$ are the real and imaginary parts of $Y$, respectively.
shown in Figure 5. There are four resonance peaks in the curve. Their corresponding frequencies are 504 Hz, 1012 Hz, 3700 Hz and 5016 Hz, respectively. The conductance values at the four resonant frequencies are 54, 691, 987 and 975 μS, respectively. The conductance amplitudes at 1012 Hz, 3700 Hz and 5016 Hz are much larger than that at 504 Hz, which explains that the latter three modes are more easily excited than the first one.

The numerical simulation result of the conductance curve of the transducer in air is shown in Figure 5. There are four resonance peaks in the curve. Their corresponding frequencies are 504 Hz, 1012 Hz, 3700 Hz and 5016 Hz, respectively. The conductance values at the four resonant frequencies are 54, 691, 987 and 975 μS, respectively. The conductance amplitudes at 1012 Hz, 3700 Hz and 5016 Hz are much larger than that at 504 Hz, which explains that the latter three modes are more easily excited than the first one.

The finite element simulation model of the piezoelectric cylindrical transducer with its frame is shown in Figure 4.

Figure 4. The finite element simulation model of the piezoelectric cylindrical transducer associated with its frame.

2.4. Input Electrical Conductance in Water

The numerical simulation result of the conductance curve of the transducer in water is shown in Figure 6. There are four resonance peaks in the curve. The frequencies of the four resonance peaks are 497 Hz, 980 Hz, 3698 Hz and 5000 Hz, respectively. The conductance values at the four resonant frequencies are 57, 582, 922, and 830 μS, respectively. The conductance amplitudes at 980 Hz, 3698 Hz and 5000 Hz are far greater than that at 497 Hz, which shows that the latter three modes are more easily excited than the first one.

Figure 5. Simulation result of input electrical conductance of the proposed transducer with the frame in air.

2.4. Input Electrical Conductance in Water

The numerical simulation result of the conductance curve of the transducer in water is shown in Figure 6. There are four resonance peaks in the curve. The frequencies of the four resonance peaks are 497 Hz, 980 Hz, 3698 Hz and 5000 Hz, respectively. The conductance values at the four resonant frequencies are 57, 582, 922, and 830 μS, respectively. The conductance amplitudes at 980 Hz, 3698 Hz and 5000 Hz are far greater than that at 497 Hz, which shows that the latter three modes are more easily excited than the first one.

2.5. Modal Analyses

According to the input electrical conductance curve of the proposed transducer in the water, as shown in Figure 6, the vibration shapes are extracted at resonance frequencies. Four main modes of the transducer in the frequency range of 0.4–6 kHz are listed in Figure 7.
2.4. Input Electrical Conductance in Water

The numerical simulation result of the conductance curve of the transducer in water is shown in Figure 6. There are four resonance peaks in the curve. The frequencies of the four resonance peaks are 497 Hz, 980 Hz, 3698 Hz and 5000 Hz, respectively, which is similar to the situation in air. The conductance values at the four resonant frequencies are 57, 582, 922, and 830 μS, respectively. The conductance amplitudes at 980 Hz, 3698 Hz and 5000 Hz are far greater than that at 497 Hz, which shows that the latter three modes are more easily excited than the first one. This is because when the transducer is immersed in water, the three bender bars are still in air in the chamber formed by cylindrical shell and endcaps.

2.5. Modal Analyses

According to the input electrical conductance curve of the proposed transducer in the water, as shown in Figure 6, the vibration shapes are extracted at resonance frequencies. Four main modes of the transducer in the frequency range of 0.4–6 kHz are listed in Figure 7.

The first mode at resonant frequency of 497 Hz corresponds to the first order of bending vibration mode of the long bender in the length direction. The second mode at resonant frequency of 980 Hz corresponds to the first order of bending vibration mode of the two short benders in the length direction. The third mode at 3698 Hz corresponds to the third order of bending vibration mode of the long bender. The fourth mode at 5000 Hz mainly corresponds to the third order of bending mode of the two short benders in the length direction.

By optimizing the design and utilizing the vibration modes in the desired frequency band effectively, the transducer can achieve better radiation performance in the whole operating frequency range.

Figure 6. Simulation result of input electrical conductance of the proposed transducer with the frame in water.

Figure 7. The vibration modes of the whole transducer at resonant frequencies of 497 Hz (a), 980 Hz (b), 3698 Hz (c) and 5000 Hz (d) in water, respectively.

The first mode at resonant frequency of 497 Hz corresponds to the first order of bending vibration mode of the long bender in the length direction. The second mode at resonant frequency of 980 Hz corresponds to the first order of bending vibration mode of the two short benders in the length direction. The third mode at 3698 Hz corresponds to the third order of bending vibration mode of the long bender. The fourth mode at 5000 Hz mainly corresponds to the third order of bending mode of the two short benders in the length direction.

By optimizing the design and utilizing the vibration modes in the desired frequency band effectively, the transducer can achieve better radiation performance in the whole operating frequency range.
2.6. Transmitting Voltage Response (TVR)

Based on our design, we fabricated and tested the transducer to compare with simulation results.

As is shown in ref [18], the transmitting voltage response level is defined with the following formula:

$$TVR = 20 \log \frac{|P_a|D}{V} + 120,$$

where $P_a$ is the pressure in the far field, usually far away from the source for about 3–5 wavelengths; $D$ is the distance from the transducer to the receiver; and $V$ is the excited voltage. The reference pressure in Equation (9) is taken to be $1 \times 10^{-6} \text{Pa} \cdot \text{m/V}$.

The comparison of the simulation and test results of TVR of the proposed transducers in water is shown in Figure 8. The solid black line and the dashed blue line represent the simulation and the test results of the proposed transducer, respectively.

![Figure 8](image_url)

**Figure 8.** Comparison of the simulation (solid black line) and test (dashed blue line) transmitting voltage response (TVR) results of the proposed transducer with the frame in water.

From the simulation of the transducer in the frequency range of 400–6000 Hz, it is shown that there are multiple resonance peaks in the curve. The four main resonant frequencies are 497 Hz, 980 Hz, 3698 Hz and 5000 Hz, respectively. The TVR values are 99 dB, 121 dB, 123 dB and 127 dB, respectively, corresponding to the four mode modes in the previous modal analysis.

In the test measurement curve, there are mainly four resonance peaks, of which the frequencies are 450 Hz, 925 Hz, 3550 Hz and 4700 Hz, respectively, and TVR values of 101 dB, 121 dB, 133 dB and 127 dB, respectively. For the resonance frequencies, the relative error between the simulation and test results is less than 11%. For the TVR value, the relative error between the simulation and test results is less than 8%. Therefore, the numerical simulation result and the test result are in good agreement.

2.7. Directivity Patterns

The numerical simulation and test measurement results of horizontal directivity of the proposed transducer at 979 Hz, 3690 Hz, and 4816 Hz are compared and shown in Figure 9. The difference between them is due to the practical measurement error because the minimum angle measurement interval is not as small as the simulation result. The results show that the performance of the developed transducer is a pure dipole source in the operating frequency range.
where $r$ and $\phi$ represent the radial and angular coordinate, respectively; $\rho$ and $c$ are the density and sound velocity of the medium, respectively; $a$ is the radius of the circular cross section; and $\omega$, $k$ and $t$ are the angular frequency, wave number and time, respectively [19].

The normalized form of the horizontal radiation pattern is:

$$DI(\theta) = \frac{(p)}{(\rho)_{\theta=0}} = |\cos \phi|$$

In theory, the dipole sound field is rather pure due to the cylindrical shell acting as the radiation surface driven by the piezoelectric actuator. Firstly, it is because of an infinite cylinder which oscillates at the velocity of $u_0(t) = u_0 e^{j\omega t}$ in the $x$-axis direction ($\phi = 0$), perpendicular to the vertical axis of the cylinder, the sound pressure (supposing the $z$-axis coincides with the vertical axis of the cylinder, then sound pressure $p$ is unrelated to the $z$-coordinate axis) can be expressed as [19]:

$$p(r, \phi, t) = \frac{-jpcu_0 e^{j\omega t}}{J_1'(ka) - jN_1'(ka)} [J_1(kr) - jN_1(kr)] \cos \phi$$

$$DI(\theta) = \frac{(p)}{(\rho)_{\theta=0}} = |\cos \phi|$$

In this work, although the cylindrical shell is finite in length, the horizontal directivity pattern can be considered similar to that of the ideal infinite cylinder because the wavelength is much more than the geometric size of the shell. Hence, the transducer with the cylindrical shell would perform as a pure dipole source. Moreover, with a full azimuthal angle of radiation surface, the vibration of the piezoelectric actuator is confined in the space of cylindrical housing so that the vibration otherwise coupling into the fluid of the borehole is effectively inhibited from interfering with the sound field [17].

![Measured and simulated directivity patterns of piezoelectric cylindrical transducer at 979 Hz (a,b), 3690 Hz (c,d), and 4816 Hz (e,f), respectively.](image)

**Figure 9.** Measured and simulated directivity patterns of piezoelectric cylindrical transducer at 979 Hz (a,b), 3690 Hz (c,d), and 4816 Hz (e,f), respectively.
borehole is effectively inhibited from interfering with the sound field by the outer radiation surface [17].

3. Comparison with Traditional Transducer

The conventional transducer is hard to generate large displacement in the frequency range below 1 kHz. This is because, for the piezoelectric material itself, the strain is so limited that it is hard to generate large displacement in the low frequency range [20]. Moreover, the radiation surface of the conventional transducer is so limited that it is hard to improve the radiation performance in low frequency.

To better understand the vibration mechanism differences between the proposed and the conventional transducers, the finite element model for the conventional transducer was established, and is shown in Figure 10. The transducer uses four long and four short piezoelectric bender bars to vibrate generating dipole signal in \( x-x \) and \( y-y \) directions. When the conventional transducer is working in one direction, i.e., the \( x-x \) direction, four bender bars which include two long bender bars facing each other and two short bender bars facing each other are excited by applying a voltage at the same time. The bender bars in our proposed transducer and conventional transducer are of same size and same materials.

![Figure 10](image-url). The \( xz \) plane sectional view of finite element model of the conventional bender-type transducer with its frame.

Through numerical simulation, the transmitting voltage response curves of the proposed transducer and the traditional transducer (XMAC tool) are shown as the black solid line and the red solid line, respectively, in Figure 11. In the numerical simulation results, the transmitting voltage response amplitudes at the resonance frequencies of 497 Hz, 980 Hz, 3698 Hz, 4118 Hz, 4692 Hz and 5000 Hz were 99 dB, 121 dB, 123 dB, 114 dB, 116 dB and 127 dB, respectively. The resonant peaks of 497 Hz and 3698 Hz corresponded to effects of the first and third order of bending vibration modes of the long bender bars, and the resonant peaks of 980 Hz and 5000 Hz corresponded to effects of the first order and third order bending vibration modes of the two short bender bars. Vibration modes at 4118 Hz and 4692 Hz, respectively, were the transition modes of the third order bending vibration mode in the length direction of the long bender bar to the third order bending vibration mode in the length direction of the two short bars. For the traditional transducer, the numerical simulated transmitting voltage response amplitudes at the resonance frequencies of 440 Hz, 920 Hz, 1120 Hz and 2720 Hz and 4260 Hz and 4890 Hz were 93 dB, 107 dB, 99 dB, 122 dB, 114 dB and 117 dB, respectively, and the resonant peaks of 440 Hz and 2720 Hz corresponded to the first and third order of bending modes of long benders in the length direction, respectively. The resonant peaks of 920 Hz and 1120 Hz mainly corresponded to the first order bending modes of the short benders in length direction. At 1120 Hz, the second order bending mode of the long benders appeared. The resonant peaks of 4260 Hz and 4890 Hz corresponded to the third order bending modes of short benders in the length.
direction. At 4260 Hz, the long benders and short benders were in the same phase, while at 4890 Hz, the long benders and short benders were in opposite phases.

![Figure 11. TVR comparison of simulation result for the proposed transducer (in black color) and the conventional bender-type transducer (in red color).](image)

The numerical analyses show that the transmitting voltage response of the newly designed transducer is 6 dB, 14 dB and 10 dB more than that of the traditional transducer at about 450 Hz, 900 Hz and 5000 Hz, respectively. From the comparison, it is concluded that the radiation performance of the proposed transducer is improved mainly because radiation surface is increased largely by using the cylindrical shell.

4. Conclusions

A new piezoelectric cylindrical acoustic transducer for dipole acoustic logging is proposed, in which three pieces of trilaminar bender bars are employed as an actuator. Based on the proposed design, the transducer was fabricated and tested. Both the numerical simulation and test results are in good agreement. From the numerical comparison analyses, it was concluded that the performance of the transducer was improved compared with the conventional transducer, especially in the frequency range lower than 1 kHz. Moreover, its directivity patterns show that this new transducer performs as a pure dipole source in the frequency range of 0.4–6 kHz, which is required in practical acoustic logging. In the present design, only three pieces of bender bars were adopted, which limits the radiation energy and vibration modes. In order to enhance the transmitting voltage response and increase the bandwidth, it is possible to add more bender bars to actuate the transducer in future application.

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References

1. Cheng, C.H.; Toksoz, M.N. Elastic wave propagation in a fluid-filled borehole and synthetic acoustic logs. *Geophysics* **1981**, *46*, 1042–1053. [CrossRef]

2. Sinha, B.K.; Kostek, S. Identification of Stress Induced Anisotropy in Formations. U.S. Patent 5398215, 14 March 1995.

3. Winkler, K.W.; Sinha, B.K.; Plona, T.J. Effects of borehole stress concentrations on dipole anisotropy measurements. *Geophysics* **1998**, *63*, 11–17. [CrossRef]

4. Tang, X.M.; Cheng, N.; Cheng, A. Identifying and estimating formation stress from borehole monopole and cross-dipole acoustic measurements. In Proceedings of the SPWLA 40th Annual Logging Symposium, Oslo, Norway, 30 May–3 June 1999.

5. White, J.E. The hula log: A proposed acoustic tool. In Proceedings of the The 8th Annual Logging Symposium, Denver, CO, USA, 12–14 June 1967; Society of Petrophysicists and Well-Log Analysts: Oklahoma City, OK, USA, 1967.

6. Winbow, G.A. Compressional and Shear Arrivals in a Multipole Sonic Log. *Geophysics* **1985**, *50*, 1119–1126. [CrossRef]

7. Zemanek, J.; Angona, F.A.; Williams, D.M.; Caldwell, R.L. Continuous acoustic shear wave logging. In Proceedings of the SPWLA 25th Annual Logging Symposium, New Orleans, LA, USA, 10–13 June 1984; Society of Petrophysicists and Well-Log Analysts: Oklahoma City, OK, USA, 1984.

8. Angona, F.A.; Zemanek, J. Shear Wave Acoustic Logging System. U.S. Patent 4649525, 10 March 1987.

9. Chen, S.T. Shear wave logging with dipole sources geophysics. *Geophysics* **1988**, *53*, 659–667. [CrossRef]

10. Hoyle, D.; Tashiro, H.; Froelich, B.; Brie, A.; Hori, H.; Sugiyama, H.; Pabon, J.; Morris, F. Dipole Logging Tool. U.S. Patent 6474439, 5 November 2002.

11. Tikalsky, C.; Chen, T.; Kainer, G.; Song, H.S. High-Performance Dipole Acoustic Transmitter. U.S. Patent 9952344, 24 April 2018.

12. Ikegami, T. Acoustic Frequency Selection in Acoustic Logging Tools. U.S. Patent 6510104, 21 January 2003.

13. Woollett, R.S.; Finch, R.D. The Flexural Bar Transducer. *J. Acoust. Soc. Am.* **1990**, *87*, 1378. [CrossRef]

14. Kim, J.; Kim, K.; Choe, S.-H.; Choi, H. Development of an accurate resonant frequency controlled wire ultrasound surgical instrument. *Sensors* **2020**, *20*, 3059. [CrossRef] [PubMed]

15. Wu, D.; Dai, Y.; Chen, H.; Zhou, Y.; Fu, L.; Wang, X. Mode analyses of trilaminar bender bar transducers using an approximation method. *Acoust. Phys.* **2017**, *63*, 617–624. [CrossRef]

16. Zhou, Y.Q.; Wang, X.M.; Xin, P.L.; Dai, Y.Y.; He, H.B.; Zhang, Z.; Yao, J.J.; Wang, Z.B. Piezoelectric Vibration Device. China Patent ZL201510042087.3, 28 July 2017.

17. Ogura, K. Apparatus for Generating P Waves and S Waves. U.S. Patent 4383591, 17 May 1983.

18. Mo, X.P. Simulation and analysis of acoustics transducers using the ANSYS software. *Tech. Acoust.* **2007**, *26*, 1279–1290. (In Chinese)

19. He, Z.Y.; Zhao, Y.F. *Acoustic Fundamental Theory*; National Defense of Industry Press: Beijing, China, 1981; pp. 221–223. (In Chinese)

20. Kim, J.; You, K.; Choe, S.-H.; Choi, H. Wireless ultrasound surgical system with enhanced power and amplitude performances. *Sensors* **2020**, *20*, 4165. [CrossRef] [PubMed]